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DRAG REDUCTION IN DILUTE FLOWING
GAS-SOLIDS SUSPENSIONS

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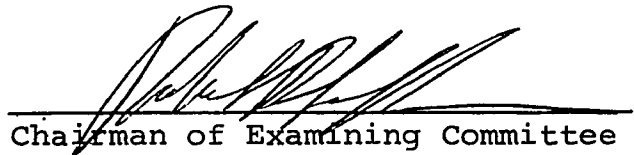
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Abstract

DRAG REDUCTION IN DILUTE FLOWING GAS-SOLIDS SUSPENSIONS

by

Ronald S. Kane

Advisor: Professor Robert Pfeffer

Flow and heat transfer measurements were performed on dilute gas-solids suspensions flowing in a closed loop system. The flow measurements included data, in the presence of particles, on gas friction factors, gas velocity profiles, gas turbulence intensity profiles, gas turbulent spectra, and particle velocity profiles. During flow measurements, glass beads ranging in size from 10 to 60 μ diameter were suspended in air at gas Reynolds numbers of 10,000 to 25,000 and solids loading ratios from 0 to 4. Pressure drop and heat transfer measurements were taken in both horizontal and vertical 0.870 inch inside diameter tubes. Profile measurements were taken only in the vertical tube. The heat transfer work included data on argon-glass and argon-aluminum suspensions.

Some novel and interesting techniques were used for measurement of various quantities in suspension flow such as solids loading ratio, gas velocity and turbulence profiles, and particle velocity profiles. A target-meter, with mounted strain gauges, was used in conjunction with a sharp-edged orifice to measure loading ratios during operation in the closed loop. A constant temperature anemometer with

thermistor and fiber-film probes was used to measure gas velocities and turbulence intensities in suspension flow. Particle velocities were measured by the double streak photographic method.

Drag and Nusselt number reduction was achieved for all particle sizes in the vertical test section. The two largest particle sizes revealed drag increases in the horizontal test section. The drag and Nusselt number results in the horizontal test section were complicated by gravity segregation and particulate deposition. The profile measurements in the vertical section indicated that the presence of particles caused a thickening of the viscous sublayer and a general increase in turbulence intensity away from the sublayer region. The gas velocity profiles for suspension flow, in the turbulent core, were similar to those without particles. The particle velocity profiles were similar, in the turbulent core, to the gas velocity profiles. A theory, based on particle-eddy interaction and viscous sublayer thickening, has been proposed to explain these results and gas-solids drag reduction in general.

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TABLE OF CONTENTS

Chapter	Page
Abstract.....	iii
ACKNOWLEDGEMENTS.....	v
LIST OF TABLES.....	ix
LIST OF FIGURES.....	x
LIST OF SYMBOLS.....	xiii
1 INTRODUCTION.....	1
2 LITERATURE REVIEW.....	6
DRAG REDUCTION--EXPERIMENTAL STUDIES AND COMMENTARIES.....	6
Decrease in Gas Viscosity.....	11
Electrostatic Effects.....	13
General Turbulence Suppression.....	16
Stabilization.....	17
Turbulence Suppression Near the Wall.....	18
DRAG REDUCTION--ANALYTICAL STUDIES.....	22
Stability Theories--Saffman; Michael; Liu.....	22
Thickening of the Viscous Sublayer--Peskin and Dwyer; Lumley; Jotaki and Tomita.....	26
NUSSELT NUMBER REDUCTION.....	28
3 LABORATORY EQUIPMENT, PROCEDURES, AND TECHNIQUES....	35
BASIC CLOSED LOOP SYSTEM.....	36
Circulator and Circulator Controls.....	36
Test Section and Upper Loop Details.....	39
Mass Flowmeter.....	42
Lower Loop and Pitot-Static Section Details.....	44
Procedures.....	46

TABLE OF CONTENTS (cont.)

Chapter	Page
OPEN LOOP CALIBRATION SYSTEM.....	47
Feed Section.....	47
Collection Section.....	49
Procedures.....	50
PRESSURE DROP MEASUREMENT SYSTEM.....	51
Equipment.....	51
Procedures.....	52
PROFILE MEASUREMENT SYSTEM.....	54
Gas Profile Measurements.....	54
Anemometer Circuitry.....	54
Probes and Probe Traverse Mechanism.....	56
Spectrum Analyzer and Voltage Regulator.....	58
Probe Calibration Procedures.....	59
Traverse Locations.....	59
Measurement Procedures.....	60
Particle Velocity Profile Measurements.....	62
Equipment.....	63
Procedures.....	63
HEAT TRANSFER MEASUREMENT SYSTEM.....	64
Test Sections and Equipment.....	64
Procedures.....	67
 4 CALIBRATIONS AND CLEAN GAS RESULTS.....	 69
PARTICLE SIZE ANALYSES.....	70
MASS FLOWMETER CALIBRATIONS.....	79
Orifice.....	79
Target-Meter.....	81
CLEAN AIR FRICTION FACTORS.....	92
ANEMOMETER PROBE CALIBRATIONS.....	95
Thermistor.....	96
Hot-Wire.....	99
Fiber-Film.....	99
CLEAN AIR VELOCITY PROFILES.....	104
CLEAN AIR TURBULENCE PROFILES.....	108
Turbulence Intensity Profiles.....	108
Thermistor.....	110
Hot-Wire.....	113
Fiber-Film.....	113
Turbulent Spectral Profiles.....	117
CLEAN GAS NUSSELT NUMBERS.....	120

TABLE OF CONTENTS (cont.)

Chapter	Page
5 SUSPENSION RESULTS AND DISCUSSION.....	124
OBSERVATIONS ON FLOW AND SOLIDS ENTRAINMENT.....	124
SUSPENSION FRICTION FACTORS.....	138
Vertical Test Section.....	139
Horizontal Test Section.....	145
Comparison to Results of Other Investigations...	145
SUSPENSION AIR VELOCITY PROFILES.....	150
SUSPENSION AIR TURBULENCE PROFILES.....	154
Turbulence Intensity Profiles.....	154
Turbulent Spectral Profiles.....	158
PARTICLE VELOCITY PROFILES.....	162
SUSPENSION NUSSELT NUMBERS.....	167
Vertical Test Section.....	168
Horizontal Test Section.....	173
Comparison to Results of Other Investigations...	177
6 THEORETICAL IMPLICATIONS AND DISCUSSION.....	179
EVALUATION OF THEORIES OF DRAG REDUCTION.....	180
Decrease of Gas Viscosity.....	180
Electrostatic Effects.....	180
General Turbulence Suppression.....	181
Thickening of the Sublayer.....	182
THEORY OF JOTAKI AND TOMITA.....	185
MODIFICATION AND EXTENSION OF THE THEORY OF JOTAKI AND TOMITA.....	197
7 RECOMMENDATIONS FOR FUTURE WORK.....	209
EXPERIMENTAL.....	210
THEORETICAL.....	212
8 SUMMARY AND CONCLUSIONS.....	215
Appendix A OPERATING INSTRUCTIONS.....	224
MASS FLOWMETER WARMUP AND SHUTDOWN.....	224
CIRCULATOR STARTUP AND SHUTDOWN.....	224
Appendix B DATA SHEETS.....	226
Appendix C NOTES ON LENGTH AND TIME SCALES.....	229
LITERATURE CITED.....	235
VITA.....	243

LIST OF TABLES

Table	Page
1 Experimental Evidence of Drag Reduction.....	7
2 Experimental Evidence of Nusselt Number Reduction....	29
3 Average Particle Diameters.....	78

LIST OF FIGURES

Figure	Page
1 Basic Closed Loop System.....	37
2 Mass Flowmeter.....	43
3 Feed and Collection Sections.....	48
4 Anemometer Circuitry.....	55
5 Closed Loop Modified for Heat Transfer.....	65
6 Size Distributions-#980 Glass Beads.....	72
7 Size Distributions-#981 Glass Beads.....	73
8 Size Distributions-#279 Glass Beads.....	74
9 Size Distributions-#380 Glass Beads.....	75
10 Size Distributions-#660 Glass Beads.....	76
11 Size Distributions-H-322 Aluminum Powder.....	77
12 Clean Gas Flow Rate Versus Circulator Shaft Speed...	82
13 Recorder Output Voltage Versus Air Drag Force.....	85
14 Recorder Output Voltage Versus Solids Drag.....	89
15 Blasius Friction Factor Versus Gas Reynolds Number..	94
16 Thermistor Calibration--Squared Voltage Versus Velocity.....	97
17 Thermistor Calibration--Voltage Versus Velocity.....	98
18 Hot-Wire Calibration--Squared Voltage Versus Velocity.....	100
19 Hot-Wire Calibration--Voltage Versus Velocity.....	101
20 Fiber-Film Calibration--Voltage Versus Velocity-- Constant Overheat.....	102
21 Fiber-Film Calibration--Voltage Versus Velocity-- Variable Overheat.....	103

Figure	Page
22 Fiber-Film Calibration--Squared Voltage Versus Velocity--Variable Overheat.....	105
23 Fiber-Film Calibration--Effect of Contamination.....	106
24 Clean Air Velocity Profiles.....	107
25 Clean Air Velocity Profiles and the Universal Velocity Profile.....	109
26 Clean Air Turbulence Intensities--Thermistor Measurements.....	111
27 Clean Air Turbulence Intensities--Hot-Wire Measurements.....	114
28 Clean Air Turbulence Intensities--Fiber-Film Measurements.....	115
29 Clean Air Turbulence Intensities Normalized with Friction Velocity.....	116
30 Clean Air Spectrum Analyses.....	119
31 Clean Gas Nusselt Number Versus Reynolds Number.....	122
32 Loading Ratio Versus Weight Added--#980 Glass Beads.	125
33 Loading Ratio Versus Weight Added--#981 Glass Beads.	126
34 Loading Ratio Versus Weight Added--#279 Glass Beads.	127
35 Loading Ratio Versus Weight Added--#380 Glass Beads.	128
36 Loading Ratio Versus Weight Added--#660 Glass Beads.	129
37 Loading Ratio Versus Weight Added--H-322 Aluminum Powder.....	130
38 Aluminum Deposit on a Thermocouple.....	135
39 Vertical Friction Factor Ratio Versus Loading Ratio--#980, #981, and #279 Glass Beads.....	140
40 Vertical Friction Factor Ratio Versus Loading Ratio--#380 Glass Beads.....	141
41 Vertical Friction Factor Ratio Versus Loading Ratio--#660 Glass Beads.....	142
42 Horizontal Friction Factor Ratio Versus Loading Ratio--#980, #981, and #279 Glass Beads.....	146
43 Horizontal Friction Factor Ratio Versus Loading Ratio--#380 Glass Beads and #660 Glass Beads.....	147

Figure	Page
44 Suspension Air Velocity Profiles.....	151
45 Suspension Air Velocity Profiles and the Universal Velocity Profile.....	153
46 Suspension Air Turbulence Intensity Profile--#380 Glass Beads.....	155
47 Suspension Air Turbulence Intensity Profile--#279 Glass Beads.....	156
48 Suspension Air Turbulence Intensities Normalized with Friction Velocity.....	159
49 Suspension Air Spectrum Analyses.....	160
50 Particle Velocity Profile.....	163
51 Vertical Nusselt Number Ratio Versus Loading Ratio--#279 Glass Beads.....	169
52 Vertical Nusselt Number Ratio Versus Loading Ratio--#380 Glass Beads and H-322 Aluminum Powder.	170
53 Horizontal Nusselt Number Ratio Versus Loading Ratio--#279 Glass Beads.....	174
54 Horizontal Nusselt Number Ratio Versus Loading Ratio--#380 Glass Beads and H-322 Aluminum Powder.	175
55 Townsend-Bakewell Eddy.....	188
56 Dimensionless Sublayer Thickness as a Function of Friction Velocity.....	199
57 Friction Factor Ratio Versus Dimensionless Sublayer Thickness Ratio.....	201
58 Friction Factor Ratio Versus Loading Ratio Based upon Modified Analysis with Variable Production...	207
59 Friction Factor Ratio Versus Loading Ratio Based upon Modified Analysis with Variable Particle Size.....	208
60 Pressure Drop and Calibration Data Sheet.....	227
61 Heat Transfer Data Sheet.....	228
62 Scaling Relationships.....	231

LIST OF SYMBOLS

a	particle diameter, ft.
a_g	gas molecular diameter, ft.
A	flow cross-section in equation 3, ft^2 .
A	characteristic velocity of attached eddy, ftsec^{-1} .
A	heat transfer surface area in equations 36-37, 39, ft^2 .
A_i	inside heat transfer surface area, ft^2 .
A_o	orifice flow cross-section in equation 11, ft^2 .
A_o	outside heat transfer surface area in equation 39, ft^2 .
A_p	pipe flow cross-section, ft^2 .
A_T	mass flowmeter target area, ft^2 .
$\bar{A}$	average heat transfer surface area for conduction, ft^2 .
B	proportionality factor in equation 31, $\text{volts ft}^{-n} \text{sec}^n$.
B	constant in equations 52 and 64, dimensionless.
c	strain gauge recorder proportionality factor, mvlbf^{-1} .
c_1	ratio of particle slip velocity at wall to characteristic velocity of attached eddy, dimensionless.
c_2	effective proportion of particles contributing to turbulent production, dimensionless.

C	orifice coefficient, dimensionless.
C_D	drag coefficient, dimensionless.
C_{pg}	specific heat capacity of gas, $\text{Btulbm}^{-1}\text{F}^{-1}$.
C_{pp}	specific heat capacity of a particle, $\text{Btulbm}^{-1}\text{F}^{-1}$.
C_{ps}	specific heat capacity of suspension, $\text{Btulbm}^{-1}\text{F}^{-1}$.
$\bar{C}$	average velocity of a gas molecule, ftsec^{-1} .
d	diameter of probe sensor, ft.
D	pipe inside diameter, ft.
e	conversion factor in equation 30, $\text{joulehrBtu}^{-1}\text{sec}^{-1}$.
f	general Blasius friction factor, dimensionless.
f_g	Blasius friction factor of clean gas, dimensionless.
f_s	Blasius friction factor of suspension based on gas properties, dimensionless.
F	total force on target, lbf.
F_g	force of gas on target, lbf.
F_p	force of particles on target, lbf.
g	gravitational acceleration, ftsec^{-2} .
g_c	gravitational constant, $\text{ftlbmlbf}^{-1}\text{sec}^{-2}$.
h_i	inside heat transfer coefficient, $\text{Btuhr}^{-1}\text{ft}^{-2}\text{F}^{-1}$.
h_o	outside heat transfer coefficient, $\text{Btuhr}^{-1}\text{ft}^{-2}\text{F}^{-1}$.
I	current in hot-wire, amps.
k	gas specific heat ratio in equation 12, dimensionless.
k	spectral wavenumber, ft^{-1} .
k^*	$k^{-1}U_*v^{-1}$, dimensionless wavenumber, dimensionless.

k_g	gas thermal conductivity, $\text{Btuhr}^{-1}\text{ft}^{-1}\text{F}^{-1}$.
k_{ss}	stainless steel thermal conductivity, $\text{Btuhr}^{-1}\text{ft}^{-1}\text{F}^{-1}$.
k_1	gas-particle velocity ratio, dimensionless.
k_2	distribution factor, dimensionless.
K	Von Karman type constant in equations 52 and 53, dimensionless.
K	Stokes drag parameter, lbmsec^{-1} .
Kn	Knudson number, ratio of molecular mean free mean path to particle diameter, dimensionless.
l	length of hot-wire, ft.
L	length of test section in equations 3, 27, 42, and 44, ft.
L	characteristic length in equations 1, 2 and stability theory description, ft.
LMTD	logarithmic mean temperature difference, F.
m	mass of one particle, lbm.
m	gain factor in equation 34, dimensionless.
n	exponent in equation 31 and factor in equations 33 and 34, dimensionless.
N	particle number density, ft^{-3} .
N_g	number density of gas molecules, ft^{-3} .
N_{T1}	Boothroyd's first time scale ratio, dimensionless.
N_{T2}	Boothroyd's second time scale ratio, dimensionless.
Nu_g	$h_i D k_g^{-1}$, Nusselt number in clean gas flow, dimensionless.
Nu_s	$h_i D k_g^{-1}$, Nusselt number in suspension flow, dimensionless.

p	local pressure, lbfft^{-2} .
p'	large eddy component of pressure, lbfft^{-2} .
p''	small eddy component of pressure, lbfft^{-2} .
P	local mean pressure, lbfft^{-2} .
P_{ads}	pressure related to acceleration of dispersed solids, lbfft^{-2} .
P_{ag}	pressure related to acceleration of gas, lbfft^{-2} .
P_{dds}	pressure related to drag of particles, lbfft^{-2} .
P_f	pressure related to combined friction of gas and dispersed solids, lbfft^{-2} .
P_{fds}	pressure related to friction of dispersed solids, lbfft^{-2} .
P_{fg}	pressure related to friction of gas, lbfft^{-2} .
P_{Lds}	pressure related to static head of dispersed solids, lbfft^{-2} .
P_{Lg}	pressure related to static head of gas, lbfft^{-2} .
P_1	pressure at upstream orifice tap, lbfft^{-2} .
P_2	pressure at downstream orifice tap, lbfft^{-2} .
Pr	$C_{pg} \mu k_g^{-1}$, Prandtl number of gas, dimensionless.
Q	heat transferred, Btuhr^{-1} .
r	radial coordinate, ft.
r	ratio of P_1 and P_2 , dimensionless
R	radius of pipe, ft.
R_v	dimensionless sublayer thickness, dimensionless.
R_{vg}	dimensionless thickness for the clean gas, dimensionless.

R_w	resistance of hot wire, ohms.
Re_g	$UD\nu^{-1}$, Reynolds number based on gas properties, dimensionless.
Re_s	Reynolds number based upon suspension properties defined in equations 1 and 2, dimensionless.
s	parameter in Saffman's theory, sec^{-1} .
s	surface area, ft^2 .
S'_{il}	large eddy component of strain rate, sec^{-1} .
t	time, sec.
t_e	characteristic time of energetic eddies, sec.
t_l	characteristic time of large eddies, sec.
t_{ss}	thickness of stainless steel tube wall, ft.
t_R	Stokesian particle relaxation time, sec.
T_g	gas temperature, F.
T_{IN}	inlet suspension temperature, F.
T_{OUT}	outlet suspension temperature, F.
T_w	hot-wire wall temperature, F.
T_{WIN}	inlet water temperature, F.
T_{WOUT}	outlet water temperature, F.
$\bar{T}_B$	average of inlet and outlet suspension temperatures, F.
$\bar{T}_W$	weighted average of wall temperature in vertical test section, F.
T'_{ds}	dispersed solids temperature fluctuation, F.
T'_f	suspending gas temperature fluctuation, F.
T'_g	clean gas temperature fluctuation, F.

u	local velocity, ftsec^{-1} .
u_i, u_ℓ	component of local velocity, ftsec^{-1} .
u'_i, u'_ℓ	component of velocity fluctuation caused by large eddy, ftsec^{-1} .
u''_i, u''_ℓ	component of velocity fluctuation caused by small eddy, ftsec^{-1} .
u'_{ds}	longitudinal velocity fluctuation of dispersed solids, ftsec^{-1} .
u'_f	longitudinal velocity fluctuation of suspending gas, ftsec^{-1} .
u'_g	longitudinal velocity fluctuation of clean gas, ftsec^{-1} .
$\overline{u_1^2}$	mean square velocity fluctuation in axial direction, $\text{ft}^2 \text{sec}^{-2}$.
U	local mean velocity, ftsec^{-1} .
U	bulk average gas velocity in equations 1, 2, 13, 14, 16-27, 44, 89, and definition of Re_g , ftsec^{-1} .
U	free stream velocity in equation 30, ftsec^{-1} .
U	overall heat transfer coefficient, $\text{Btuhr}^{-1} \text{ft}^2 \text{F}^{-1}$.
U_c, U_ℓ	local mean velocity at centerline, ftsec^{-1} .
U_i, U_ℓ	component of local mean velocity, ftsec^{-1} .
U^*	characteristic velocity in stability theory, ftsec^{-1} .
U_*	$(g_c \tau_w \rho^{-1})^{1/2}$, friction velocity, ftsec^{-1} .
v_i	local particle velocity, ftsec^{-1} .
v'_{ds}	transverse dispersed solids velocity fluctuation, ftsec^{-1} .
v'_f	transverse suspending gas velocity fluctuation, ftsec^{-1} .

v'_g	transverse gas velocity fluctuation, ftsec^{-1} .
V	anemometer voltage without gain and zero adjustment, volts.
V	volume in integral equations, ft^3 .
V_a	anemometer voltage with gain and zero adjustment, volts.
V_{arms}	root mean square anemometer voltage with gain, volts.
V_o	no-flow anemometer voltage at variable temperatures, volts.
V'_o	no-flow anemometer voltage at a fixed temperature, volts.
V_p	bulk average particle velocity, ftsec^{-1} .
V_{pc}	particle centerline velocity, ftsec^{-1} .
V_{pcs}	particle centerline slip velocity, ftsec^{-1} .
V_{ps}	bulk average particle slip velocity, ftsec^{-1} .
V_{pw}	particle slip velocity at wall, ftsec^{-1} .
V_{rms}	root mean square anemometer voltage, volts.
V_t	particle terminal settling velocity, ftsec^{-1} .
V_{ST}	recorder output voltage, mv.
V_{STO}	recorder output voltage for gas flow only, mv.
W_g	mass flow rate of gas only, lbmsec^{-1} or lbmhr^{-1} .
W_p	mass flow rate of particles only, lbmsec^{-1} or lbmhr^{-1} .
W_s	mass flow rate of suspension, lbmsec^{-1} or lbmhr^{-1} .
x_i, x_ℓ	general coordinate, ft.
x_1	longitudinal coordinate, ft.

x_2	transverse coordinate, ft.
x_3	spanwise coordinate, ft.
y	distance from pipe wall, ft.
y^*	$yU_*\nu^{-1}$, dimensionless distance from pipe wall, dimensionless.
Y	expansion factor, dimensionless.
α	parameter of Townsend attached eddy, ft^{-1} .
β	ratio of orifice to upstream tube diameter, dimensionless.
δ	ratio of specific heat of particle to specific heat of gas, dimensionless.
δ_v	thickness of viscous sublayer, ft.
ϵ	turbulent production per unit mass, $\text{ft}^2\text{sec}^{-3}$.
η	loading ratio, dimensionless.
η	equilibrium range length scale in Appendix C, ft.
η^*	dimensionless equilibrium length scale, dimensionless.
λ	length scale, ft.
λ^*	dimensionless length scale, dimensionless.
μ	micron
μ	gas viscosity, $\text{lbmft}^{-1}\text{sec}^{-1}$.
ν	$\mu\rho^{-1}$, gas kinematic viscosity, $\text{ft}^2\text{sec}^{-1}$.
ν_T	eddy kinematic viscosity, $\text{ft}^2\text{sec}^{-1}$.
ρ	gas density, lbmft^{-3} .

ρ_{ds}	dispersed solids density, lbmft^{-3} .
ρ_p	density of a particle, lbmft^{-3} .
ρ_t	density of fluid in which particle terminal settling velocity is measured, lbmft^{-3} .
ρ_1	gas density upstream of orifice, lbmft^{-3} .
τ	time scale, sec.
τ^*	dimensionless time scale, dimensionless.
τ_w	wall shear stress, lbfft^{-2} .

Chapter 1

INTRODUCTION

The suspension of particulate matter in a gaseous medium is an extremely common although often unnoticed phenomenon. The smoke from a cigarette, the windborn conveyance of plant pollen, the exhaust from a power-plant smokestack are everyday examples of gas-solids suspensions. Industrial applications include pneumatic transport of granular sugar from tank trucks to commercial consumers (Tills 1971), pneumatic supply of grain and cereal products to storage silos, and circulation of fluidized, solid catalysts in the catalytic cracking and coking units of oil refineries. Less prosaic and still unperfected applications include use of gas-solids suspensions in gas-core nuclear rockets and coal-seeded natural gas pipelines.

Despite voluminous literature on the properties of gas-solids suspensions, the scientist and engineer must often resort to empiricism and guesswork for required information. The problem is particularly acute for the normally turbulent confined flows in pipes and channels. Much excellent experimental work has been published; yet, unless conditions in a laboratory test closely parallel those in a proposed application, no assurance can be provided that the numerical results or even the trends of the test will correctly describe the behavior of the application. Because the flow and heat transfer properties of a Newtonian fluid can be characterized

by relatively few parameters such as the Reynolds and Prandtl numbers, its behavior is usually predictable. However, the introduction of solids in suspension also introduces a large number of possible scaling parameters and additional dimensionless groups. To make matters more complicated, not all these new parameters and groups need be considered for any given situation.

Whereas the flow of a clean Newtonian fluid can be considered to be in either the laminar or turbulent flow regime, the flow of a suspension must be further subdivided into additional regimes dependent upon the behavior of the particles. These regimes can be characterized by the importance of various particle flow phenomena: Brownian diffusion, gravity segregation, saltation, deposition, turbulence interaction, and electrostatic charging. These phenomena, in turn, are governed by various particle, gas and flow properties; particle size, shape, density, elasticity, thermal conductivity, heat capacity, and electrical conductivity, gas density, viscosity, thermal conductivity and heat capacity, and carrying velocity, Reynolds number, mass loading ratio, pipe diameter, roughness, and material. The most important of the parameters categorizing suspension flow are particle size, mass loading ratio, and gas Reynolds number (Boothroyd 1971:65).

It is apparent that the only feasible approach for an investigation in this field is to select one of the regime-characteristic flow phenomena for detailed study. This dissertation encompasses the limited regime of turbulence interaction where the phenomena of drag and Nusselt number reduction in flowing gas-solids suspensions have been observed. According to a recent review paper (Owen 1969),

turbulence interaction during pneumatic transport is important in the, approximate, 10 to 100 μ particle diameter size range and the 15 to 300 feet per second carrying velocity range. Therefore, a study of drag reduction provides information on the intermediate range of particle sizes where Brownian diffusion at the lower limit and deposition at the upper limit are not important.

Drag reduction has been observed by few workers, mainly because it appears at low loading ratios. (The loading ratio is defined as the relative mass flow rate of the solids to the mass flow rate of the conveying fluid.) The original impetus to experimental investigation of suspensions was provided by the petrochemical industry for application to dense phase or high loading ratio fluidized bed processes. The petrochemical industry experience indicated that addition of particulate matter to a clean gas leads to a sizeable increase in the pressure drop required to maintain a given flow rate of the fluidizing medium. Under the same conditions of heavy particulate loading, suspension heat transfer coefficients were found to be much larger than clean gas heat transfer coefficients at comparable gas flow rates. Laboratory measurements (Wen and Simons 1959; Hariu and Molstad 1949; Farbar and Morley 1957) of the properties of dense flowing suspensions agreed with the early industrial results. Because the Einstein equation (Batchelor 1967:253) predicts an effectively increased suspension viscosity for very dilute suspensions, extrapolation of the industrial results to zero loading ratio yielded a monotonic approach (from higher values) of the suspension properties to that of the clean gas.

However, several investigators (Boothroyd 1966; Soo and Trezek 1966; Rossetti and Pfeffer 1972) have found that addition of small quantities of particulate matter to a turbulently flowing gaseous stream decreased the friction factors of the gaseous phase below that of the clean gas. Other investigators (Tien and Quan 1962; Farbar and Depew 1963; Jepson, Poll, and Smith 1963) have reported a significant decrease in Nusselt numbers at loading ratios near unity. At higher loading ratios, all investigators reported an increase in friction factors and Nusselt numbers. Although these results indicated a gaseous analogue to the well documented (Savins and Virk 1971) phenomenon of liquid drag reduction, scant attention was paid to the possibility.

This dissertation contains the results of a three phase experimental investigation into the existence, causes, and effects of drag reduction in dilute flowing gas-solids suspensions. The first phase consisted of friction factor measurements with the aim of definite confirmation of gaseous drag reduction, without recourse to unverified experimental assumptions. The second phase consisted of gas velocity, gas turbulence intensity, gas turbulent spectra, and particle velocity profile measurements designed to elucidate the reasons for the drag reduction. The third phase consisted of heat transfer measurements with the aim of providing an analogy between drag reduction and Nusselt number reduction. The results were used to develop a theory predicting the extent of the drag reduction.

During phases one and two, five different nominal sizes of glass beads ranging from 10 to 60 μ in diameter were suspended in air at gas Reynolds numbers of 10,000 to 25,000 and solids loading ratios from 0 to 4. The frictional

pressure drop of air in two 0.870 inch, inside diameter, stainless steel test sections, one vertical and one horizontal, was measured for both clean gas and suspension flow. A reduction of the air friction factor in suspension flow below that of the friction factor in clean gas flow at the same gas Reynolds number was defined as drag reduction. Because gravity segregation could be discounted in the vertical test section, all profile measurements were restricted to the vertical test section.

During phase three, only two glass bead sizes were used in air. However, experiments were also performed with pure argon, glass suspended in argon, and aluminum powder suspended in argon. Heat transfer measurements were taken in a cooled horizontal test section and a heated vertical test section. In analogy with the definition of drag reduction, Nusselt number reduction was defined at equivalent gas Reynolds numbers.

One of the difficulties attending experimental investigations of gas-solids suspensions has been the general unsuitability of many instruments that otherwise perform adequately in clean gas service (Boothroyd 1971:69-99). As a byproduct of this investigation, some novel and interesting measurement techniques were developed and refined. These techniques included use of a target-meter and orifice to measure the solids loading ratio in a closed loop system, a fiber-film probe and a constant temperature anemometer to measure gas mean velocity, gas turbulent intensity and spectral profiles, in suspension flow, and a high speed strobe and camera to measure particle velocity profiles.

Chapter 2

LITERATURE REVIEW

Only a handful of articles have been published which specifically treat gas-solids drag or Nusselt number reduction. More commonly the phenomenon has been mentioned as an unusual feature of part of a flow experiment with gas-solids suspensions. This chapter provides summaries of all the important literature published in this field and includes a description of theories developed from both experimental and analytical studies.

DRAG REDUCTION-EXPERIMENTAL STUDIES AND COMMENTARIES

Gas-solids drag reduction, defined as a reduction of the friction factor of the gaseous phase below that of the clean gas at corresponding gas mass flow rates, has been observed under a variety of conditions. A summary of the experimental evidence of drag reduction is shown in Table 1. In general particle sizes were small and Reynolds numbers were low although the flow was always turbulent. The low values of these parameters may have been the result of the physical restrictions on the experimental apparatus and not the result of basic restrictions on the extent of drag reduction. Certain scale factors pointed out by Boothroyd and Saffman (and described later) are probably more important in delimiting the range of drag reduction than are particular

Table 1
Experimental Evidence of Drag Reduction

Reference	Solids Diameter and Type	Pipe Diameter and Orientation	Loading Ratio	Reynolds Number
Segler 1951	wheat grains	9" horizontal	0-30.0	200,000- 250,000
Clark et.al. 1952	1060 μ cress	1" horizontal	1.5-5	12,000- 68,000
Halstrom 1953	200 μ glass	-----	----	-----
Depew 1960	30 μ glass	0.71" vertical	0-5.0 0-3.5	13,500 27,400
Richardson et.al. 1960	1460 μ perspex	1" horizontal	0-1.0	12,000- 70,000
Schluderberg et.al. 1961	1-5 μ graphite	0.532" vertical	0-12.0	20,000- 200,000
Sproull 1961	-----	-----	0.21	-----
Hawes et.al. 1964	0-5 μ graphite	0.5" vertical	----	-----

Table 1 (continued)

Reference	Solids Diameter and Type	Pipe Diameter and Orientation	Loading Ratio	Reynolds Number
Boothroyd 1966	0-40μ zinc	1" vertical	0-3.0	35,000
			0-7.0	53,000
			0-12.0	80,000
			0-12.0	100,000
	2" vertical	0-2.0	35,000	
		0-2.5	53,000	
		0-2.5	80,000	
		0-2.5	100,000	
	3" vertical	0-1.0	53,000	
		0-1.5	80,000	
Soo et.al. 1966	30μ magnesia	5" horizontal	0-1.0	130,000- 295,000
Boyce et.al. 1970	2-60μ silica	2.75" angulated	0-3.5	18,200- 63,000
	100μ glass			9,450- 63,000
	200μ glass		0-4.0	12,000- 63,000
	840μ glass			40,400- 63,000
	1680μ glass		0-6.0	13,800- 63,000

Table 1 (continued)

Reference	Solids Diameter and Type	Pipe Diameter and Orientation	Loading Ratio	Reynolds Number
Mason et.al. 1971	15,40,70 μ alumina	1" vertical	0-6.0	140,000
	15 μ alumina		0-4.0	75,000
	15 μ alumina		0-1.0	57,000
	15,40 μ alumina	2" vertical	0-1.0	70,000
	15 μ alumina		0-1.0	85,500
			0-1.0	105,000
	15,40,70 μ alumina	3" vertical	0-1.5	69,500
Peters et.al. 1972	25 μ glass	1" vertical	0.8-1.4	15,000-25,000
Rossetti et.al. 1972	10 μ glass	1" vertical	0-1.5	24,250
	20 μ glass		0-0.4	13,150
			0-0.7	16,700
			0-2.0	22,750
	25 μ glass		0-0.4	13,000
			0-0.6	16,250
			0-1.8	22,800
	34 μ glass		0-0.7	12,000
			0-0.9	15,450
			0-1.5	22,300
	59 μ glass		0-0.6	12,500
			0-0.7	16,400
			0-1.0	21,800
	10 μ glass	0.870" horizontal	0-0.7	27,700

Table 1 (continued)

Reference	Solids Diameter and Type	Pipe Diameter and Orientation	Loading Ratio	Reynolds Number
Rossetti et.al. 1972 (continued)	20 μ glass	0.870" horizontal	0-0.4	15,000
			0-0.6	19,100
			0-2.0	25,925
	25 μ glass		0-1.0	18,800
			0-2.5	25,900

values of particle size, pipe diameter, and Reynolds number.

The results shown in the table have been used by the experimental investigators and others to postulate causes of drag reduction. These causes can be placed in the following broad categories: decrease in gas viscosity, electrostatic effects, general turbulence suppression, stabilization, and turbulence suppression near the wall. (This last category is equivalent to thickening of the sublayer described in a later section.) The five possible causes are not mutually exclusive as all relate to the idea that the particles interfere with the turbulent structure of the gas producing a reduction in skin friction. This section describes the experimental literature in terms of these five causes.

Decrease in Gas Viscosity

The reduction of total pressure drop with the addition of dust to the flow of air in a pipe or cyclone (as measured by Briggs in 1945) was reported by Sproull (Sproull 1958; Sproull 1960; Sproull 1961; Sproull 1962). His interest in this phenomenon led him to measurements of the viscosity of a gas-solids suspension using parallel disk and coaxial cylinder viscosimeters. He concluded that suspended limestone and talc dust had the effect of decreasing the mean free path of the suspending gas and significantly reducing the viscosity of the gas. However, his explanation, a reduction of the gas viscosity, for the observed decrease in pressure drop in the pipeline tests by Briggs was discounted by Saffman (Saffman 1962b) and by Chand (Chand 1970) as contradicting the Einstein equation. Saffman showed approximately and Chand rigorously that the reduced viscosity measured by the viscometer could be explained more logically by

considering the effect of the momentum of the dust on the velocity gradients in the viscosimeter.

In a two-part program Boyce and Blick (Boyce and Blick 1970) measured drag reduction in an angularly oriented, 2-3/4 inch bore pipe and on a flat plate. The authors correlated their data on one curve, for each of the two experiments, by redefining the Reynolds number in terms of revised mixture properties. The viscosity of the suspension was chosen from one of two formulae, depending upon the value of the particle Knudsen number. For Knudsen numbers approaching zero, for large particles, the Einstein equation was considered valid and was used. For Knudsen numbers greater than 0.01, for small particles, the authors used admittedly questionable reasoning (justified as giving the best correlation) to apply the kinetic theory of a two component gas (as expressed by Sproull) to calculate the viscosity. The density-velocity product in the Reynolds number was represented by the sum of the density-velocity product of the air and the dispersed density-particle velocity product of the solids. The final form of the Reynolds number was

$$Re_s = \frac{(P_{ds}V_p + \rho U)L}{\mu \left(1 + \frac{5}{12} a^3 N\right)} \quad \text{for } Kn \rightarrow 0 \quad (1)$$

and

$$Re_s = \frac{(P_{ds}V_p + \rho U)L}{\frac{1}{3} \rho \bar{c} \left[\sqrt{2} \pi (N_g a_g^2 + N a^2) \right]^{-1}} \quad \text{for } Kn > 0.01 \quad (2)$$

The first formula was applied to 840 and 1680 μ particles; the second to smaller particles. The correlation resulting from these formulae was remarkable as all data of skin friction coefficient versus these Reynolds numbers fell on a single curve

regardless of gas Reynolds number, particle size, and loading ratio. Nevertheless the authors could not justify theoretically their use of increased viscosities for suspensions of large particles and decreased viscosities for suspensions of small particles.

Electrostatic Effects

In one of the earliest papers mentioning drag reduction (Clark, Charles, Richardson, and Newitt 1952), the authors reported the results of pneumatic conveying experiments with aluminum, brass, cress, coal, and perspex in a horizontal 1 inch bore brass tube. The pressure drop in the test section was divided into two parts, one due to the solids and the other due to the air. The pressure drop due to the solids was given by

$$\Delta P_{dds} = \frac{L W_p}{A V_p} \left(1 - \frac{\rho_t}{\rho_p}\right) \left(\frac{\rho}{\rho_t}\right) \left(\frac{U - V_p}{V_t}\right)^2 \frac{g}{g_c} \quad (3)$$

derived from considerations of the drag force of the air stream on the particles. The authors were able to measure the average solids velocity and could, therefore, calculate the solids pressure drop directly. The equation could not predict the additional pressure drop caused by direct contact friction between the solids and the wall.

The pressure drop due to the air was assumed to be the same as that of a clean air flow (and calculated by standard friction factor equations). The authors had hoped that the sum of the two calculated pressure drops would agree with the measured total pressure drops. A comparison was performed for the 1060 μ cress; and, rather than underpredicting the measured total pressure drop, the combination of the

solids pressure drop equation and the standard friction factor equation overpredicted the measured total pressure drop. Because the authors felt that the calculated solids pressure drop was an underestimate, they concluded that the presence of the particles affected the air turbulence and reduced the fluid skin friction.

During a subsequent experiment, (Richardson and McLeman 1960) intended to study the development of electrostatic charges and their effect on two-phase pressure drops, the authors continuously recycled suspensions of brass, aluminum, lead, polystyrene, and several grades of perspex and coal. They found that the pressure drop in most cases increased with increasing time of circulation. For these cases, the authors postulated that the suspended particles and the dust layer on the wall gradually accumulated opposing electric charges which attracted the suspended particles to the wall.

However, one material, 1460 μ perspex, caused a reduction in pressure drop which did not increase with time. The authors suggested that the suspended perspex particles and the dust layer on the wall had acquired like charges forcing the particles to travel near the axis of the pipe. There was no explanation for the assumption that opposing charges of themselves produce drag increases or that like charges of themselves produce drag decreases.

As part of his survey article on pneumatic transport, Owen (Owen 1969) chose the work of Clark, Charles, Richardson and Newitt as a point of departure to summarize his thoughts on the reduction of pressure drop accompanying the presence of particles. He suggested several explanations for the phenomenon depending on the particle size and the existence of electrostatic charges.

For the case of large particles where the electrostatic charge density would not be important, two alternative explanations were proposed. First, the particles could act to homogenize the turbulence in the sense of a gauze screen by lagging behind the gas flow, that is slowing down the faster moving parts of the flow and speeding up the slower moving parts. The second was based on the fact that measurements often indicated a reduction in skin friction and not necessarily a reduction in total pressure drop. The gas apparently contributed a large proportion of the shear stress to maintaining the large particles in suspension (translated into form drag) at the expense of the shear stress at the wall. The net effect might be to reduce the skin friction without reducing the pressure drop.

For the case of small particles, Owen invoked the presence of presumed high electrostatic charge density to account for the unmistakable reduction in transport of momentum (observed by Soo) and heat (observed by Tien and Quan). Because deposition did not occur in the two experiments, (Soo and Trezek 1966; Tien and Quan 1962) Owen suggested that part of the turbulent energy production had been expended to counteract the drift of particles toward the wall and to avoid consequent deposition. He based this hypothesis on the work of Soo (Soo, Trezek, Dimick and Hohnstreiter 1964) which showed a pronounced increase in particle concentration near the wall. By comparing the rate of work done by Reynolds stresses against the mean flow in the wall region with the work required to counteract the drift of particles to the wall, Owen derived a form of the Richardson number. (The Richardson number is often used to describe stability in a stratified flow situation.) The order of the Richardson

number for the experiments by Soo (Soo and Trezek 1966) were of the same order as those determined by other workers as indicative of changes in turbulence structure in single phase stratified flows. In another section of his paper, Owen suggested that the effect of small uncharged particles was to increase the dissipation without additional turbulent production from their wakes. However, he did not relate the increased dissipation to the drag reduction phenomenon.

General Turbulence Suppression

During tests of graphite suspensions as heat transfer fluids, it was discovered (Schluderberg, Whitelaw, and Carlson 1961; Babcock and Wilcox 1960a; Babcock and Wilcox 1960b) that dilute suspensions of 1-5 μ particles in circular channels produced lower friction factors than predicted by the Moody diagram. The authors suggested that the particles had suppressed the turbulence of the flow at higher Reynolds numbers and extended laminar flow behavior into the normally turbulent regime.

As part of a series of measurements on the concentration distribution of 36 μ average diameter magnesia particles suspended in air, Soo and Trezek (Soo and Trezek 1966) noted that the addition of solid particles led to a decrease of gas friction factors. This phenomenon was restricted to low values of loading ratio, below unity, and disappeared at higher values. The suspension was circulated in a horizontal 5 inch brass pipe at Reynolds numbers between 130,000 and 295,000. The authors noted, as had Boothroyd, that an initial thin insulating layer deposited on the walls and limited the buildup of electrostatic charges. This deposit constituted an initial roughness which gave a 10 to 12

percent increase in friction factor for flow of air alone. In his book (Soo 1967:144), Soo attributed the decrease in the gas friction factor with particle addition (which overcame a rougher wall as well) to a reduced mixing length due to a dissipation by solid particles.

Stabilization

As part of his discussion of Sproull's paper, Saffman (Saffman 1962b) offered an explanation for the observed pressure drop reduction in pipes and cyclone systems. He suggested that dust particles having a relaxation time comparable with or greater than the time-scale of the turbulence could provide an additional energy dissipative mechanism by virtue of a finite relative velocity between the particles and the turbulent gas. The additional dissipation would damp the turbulence, reduce the Reynolds stresses, and finally reduce the pressure drop. By contrast, particles with small relaxation times would closely follow the turbulent motion of the gas and have the effect of increasing the fluid density and increasing the Reynolds stresses.

The drag reduction reported by Rossetti and Pfeffer (Rossetti 1969; Pfeffer and Rossetti 1971; Rossetti and Pfeffer 1972) was explained on the basis of the theory of Saffman (Saffman 1962a). The particle relaxation time:

$$t_R = \frac{a^2 \rho_p}{18\mu} \quad (4)$$

was compared to two characteristic times, the one for the energy containing eddies:

$$t_e = \frac{0.1D}{2U_*} \quad (5)$$

and the one for the large eddies:

$$t_l = \frac{D}{2U_*} \quad (6)$$

All the particles seemed to have sufficiently long relaxation times, that is longer than the characteristic time of the energetic eddies, to increase the stability of a dusty gas and hence lead to drag reduction. The stabilizing effect would increase with increasing particle size and relaxation time until the relaxation time of the particle became much greater than the characteristic time of the large eddies or until the particle terminal settling velocity approached the friction velocity (particularly in horizontal flow). Too large a particle would actually lessen the stabilizing effect.

These qualitative conjectures fit the data quite well as the measured drag reduction increased with increasing particle size. The drag reduction appeared to reach a limit with the largest particle sizes considered, 34 and 59 μ diameter. The 34 μ particles actually gave a slightly greater amount of drag reduction than the 59 μ particles. This may have been the result of achievement of an optimum size for drag reduction or to inaccuracy in the measurement of loading ratio.

Turbulence Suppression Near the Wall

In order to test a continuum model of a gas-solids suspension, McCarthy and Olson (McCarthy and Olson 1968) measured the pressure drop of suspensions of 147-417 μ lucite beads, 53-74 μ glass beads, and 1-10 μ calcium carbonate flakes in a 1 inch bore horizontal pipe at gas Reynolds numbers between 100,000 and 1,000,000 and loading ratios between 0

and 0.6. They reported reduced friction factor ratios for all flow conditions. However, the calculation of friction factors in suspension flow was based upon the density of an air-solid continuum. The suggested drag reduction was probably the result of the data reduction rather than an alteration of the flow properties. Nevertheless, the total pressure drop of the system did not increase upon addition of solids to the gas. Because their measurements indicated that the particles did not affect the gas velocity profile in the central region of the pipe, they implied that the structure of the turbulence in the core was unaffected by the presence of particles. The authors felt that the lack of pressure drop increase was the result of reduced turbulence in the regions closer to the wall.

An appraisal of the phenomenon of drag and Nusselt number reduction was made by Boothroyd and reported in a series of articles (Boothroyd 1966; Boothroyd 1967; Haque and Boothroyd 1969; Boothroyd and Haque 1970; Mason and Boothroyd 1971) which were eventually incorporated into a book (Boothroyd 1971). The first article described the marked reduction of the frictional and total pressure drop caused by the addition of 0-40 μ zinc dust to turbulently flowing air. The experiments were carried out in 1, 2, and 3 inch vertical tubes at loading ratios between 0 and 20 and air Reynolds numbers between 35,000 and 100,000.

Boothroyd found that the friction factor ratio (the ratio of the combined solids and gas frictional pressure drop with solids to that without solids at corresponding air flow rates) could be correlated with the loading ratio and a time scale ratio:

$$\frac{\rho D^2}{\rho_p a^2 Re_g}$$

The time scale ratio represented the ratio of the fluid eddy time to the Stokesian particle relaxation time. The Reynolds number, although required for dynamic similarity of the gas phase at low solids loadings, was experimentally found to have negligible effect on the friction factor ratio.

With the exception of experimental data for the highest values of the time scale ratio in the largest pipe, distinct minima in the curves of friction factor ratio versus loading ratio appeared at loading ratios between 1 and 2.2. The large reduction of friction factor, as much as 75 percent in the 1 inch pipe, was explained with reference to the time scale ratio and the effect of particles on the turbulent energy balance of the fluid. Small particles could not produce turbulence from their wakes so that any increase in turbulent energy production would have to be the result of increasing the effective density of the flow. However, the same small particles could also produce additional dissipation in the fluid by being too large to follow rapid eddy motions, particularly near the wall. Assuming the particles are not so large that Stokesian type drag is an incorrect description of their behavior, the time scale ratio provides a qualitative measure of the predominance of particle turbulence production or dissipation. High values of the time scale ratio (large eddies and rapidly responding particles) favor turbulence generation and increased friction factors. Low values of the time scale ratio (small eddies and slowly responding particles) favor turbulent dissipation and reduced

friction factors. These trends described the trends of Boothroyd's results exactly.

In contrast to the work of Richardson and McLeman, Boothroyd did not find any great effect of electrostatic charges upon his results. He postulated that the very fine particles provided a thin coating on the pipe wall so that the surface properties of the wall and the particles became identical. This arrangement inhibited charge transfer and allowed uniform and reproducible pressure drop measurements.

The results of the pressure drop experiments suggested the need for measurement of the turbulence characteristics of the suspension under drag reducing conditions. Boothroyd attempted (Boothroyd 1967) to measure the gas eddy diffusivity, in the presence of solids, using gas tracer techniques. While attempts to establish relationships between the gas eddy diffusivity and the time scale ratio, loading ratio, and gas Reynolds number were entirely unsuccessful, qualitative results were reported. The dimensionless eddy diffusivity was found to be higher in the 3 inch pipe than in the 2 inch pipe. These results were consistent with the higher friction factor ratios observed in the 3 inch pipe compared to those observed in the 2 inch pipe, as reported in the previous paper. However, the measured eddy diffusivity was an apparent overall coefficient based on tracer samples taken in the central region of the pipe. Boothroyd was unable to determine the variation of eddy diffusivity with the radial dimension and could not determine if the presumed turbulence suppression for the drag reducing case was restricted to the near wall region or was generalized to the entire pipe.

He postulated that the solids would attenuate turbulent production near the wall and aid turbulent production far

from the wall. The characteristic eddy time scale at the center of the pipe is much larger than the characteristic eddy time scale near the wall. Particles would be more responsive to the larger eddy time scales at the pipe center than to the smaller time scales near the wall. The variation of friction factor ratio with pipe diameter (lower for smaller pipes) was considered consistent with the decreased eddy time scales of the smaller pipes and the reduced ability of fixed particle sizes to respond to these smaller eddies.

DRAG REDUCTION-ANALYTICAL STUDIES

The effect of dust upon the turbulence characteristics of a gas has been treated in two contexts. The first, calculations of the stability of laminar plane flow of a dusty gas, has been treated by analogy as a qualitative explanation of gas-solids drag reduction. The second, estimation of the effect of dust on the thickness of the viscous sublayer, is of more direct application to the phenomenon as observed in pipes.

Stability Theories--Saffman; Michael; Liu

As a sequel to the discussion of Sproull's measurements of the viscosity of dusty gases, Saffman (Saffman 1962a) formally modified the Navier-Stokes equations to account for the presence of dust. He assumed that the volumetric concentration and the size of the particles were so small that the net effect of the dust was to add a Stokesian drag to the equation. The assumption of small volumetric concentration essentially limited the analysis to gaseous flow. (For liquids, loading ratios corresponding to those commonly observed in dilute gaseous suspensions would require large

volumetric concentrations and significant correction to the liquid viscosity as predicted by the Einstein equation.) Saffman observed that one effect of a linearized perturbation analysis on the Navier-Stokes equation for the gaseous phase and the equation of motion for the solid phase was the appearance of two controlling parameters. One parameter, s , the ratio of (1) the dispersed solids to gas density ratio to (2) the particle relaxation time, appeared in the equation for the fluid velocity perturbations. The other parameter, t_R , the particle relaxation time, appeared in the equation for the particle velocity perturbations.

Denoting the length and velocity scales of the problem as L and U^* , several general conclusions could be drawn regarding limiting values of the relaxation time. For $t_R \ll L/U^*$ (fine dust), the particle velocity equation indicated that the fluid and particle velocity perturbations were of the same order of magnitude. The corresponding simplification in the fluid perturbation velocity equation revealed that the effect of fine dust was to increase the effective density of the fluid and hence decrease the value of the critical Reynolds number based on clean gas properties.

For $t_R \gg L/U^*$ (coarse dust), the particle velocity perturbation was negligible compared to the fluid velocity perturbation. The corresponding simplification in the fluid velocity perturbation revealed that the effect of coarse dust was to add another frictional force to the fluid. The additional force was proportional to the ratio, s , and was also proportional to the fluid perturbation velocity.

The simple addition of the coarse particles, therefore, provided a stabilizing influence. Increasing the value of s increased the stabilizing tendency. For a fixed value of the

density ratio, the stabilizing effect of coarse dust would actually increase with decreasing particle size and relaxation time. Unfortunately the increase in stability was limited by the failure of the assumption $t_R \gg L/U^*$ if the relaxation time became so small that the dust could no longer be considered coarse. An optimum particle size seems to be implied by this reasoning.

Saffman stated that the presence of particles would not add new modes of instability to the gas but only modified the old ones. He proceeded to prove that Squire's theorem, that a two dimensional disturbance rather than a three dimensional disturbance controls the critical Reynolds number, was valid for a dusty gas and formulated the Orr-Sommerfeld equation for arbitrary values of t_R . In a simple example he showed that dust destabilizes those disturbances with large wavelengths and stabilizes those with small wavelengths.

The solution of the Orr-Sommerfeld equation posed by Saffman was attempted by Michael (Michael 1964) for plane Poiseuille flow. Because of the mathematical complexity of the problem, the solution was approximated as a perturbation off the clean gas neutral stability curve for a small value of dispersed solids to gas density ratio. At low values of $t_R \frac{U^*}{L}$, the neutral stability curves for the gas indicated a lower critical Reynolds number in accordance with Saffman's conjecture. As the value of $t_R \frac{U^*}{L}$ increased, the critical Reynolds number increased and the maximum wavenumber for instability decreased. This result indicated that the particles were becoming less sensitive to small scale (high wavenumber) disturbances. This was consistent with Saffman's view that larger particles are less able to follow the small scale motions of the fluid. At high values of $t_R \frac{U^*}{L}$, the

trends reversed indicating that the particle population was now too small to stabilize the flow. In another paper, Michael (Michael 1965) showed that the presence of dust has the effect of reducing the growth rate of disturbances from a vortex sheet but is unable to stabilize them.

Liu (Liu 1965) simplified the disturbance equations formulated by Saffman to calculate the gas and particle Reynolds stresses in the near wall region with the view of determining the effect of particles upon the production of disturbance kinetic energy. Particles were considered to have a dual role in augmenting the viscous dissipation that damps disturbance kinetic energy and in adding to production of disturbance kinetic energy by the action of Reynolds stresses working against the mean velocity gradient. The solution of the disturbance vorticity equation and the gas continuity equation were used to compute the disturbance velocity components and the disturbance Reynolds stresses.

The particles affected both the magnitude of the individual velocity components and the phase relationships between them. The combination of the two determined the value of the disturbance velocity correlations. Two dimensionless parameters, (1) the dispersed solids to gas density ratio and (2) the inverse product of the disturbance cyclic frequency and the particle relaxation time, defined the solutions. The solutions were plotted as the ratio of the two phase Reynolds stress to the clean gas Reynolds stress versus the second parameter. At low values of the second parameter (high frequencies or large relaxation times), the Reynolds stress ratio decreased below unity. At higher values of the parameter the Reynolds stress ratio increased to an asymptotic maximum. The dispersed solids to gas

density ratio seemed to have little effect on the Reynolds stresses at low values of the second parameter but uniformly increased the Reynolds stresses at high values of the parameter.

Liu did not calculate the value of the kinetic energy production or the dissipation but the implications were clear. High frequencies and long relaxation times were conducive to net decrease of disturbance kinetic energy in the near wall region. Low frequencies and short relaxation times were conducive to increases in the turbulent production of kinetic energy although the additional production was not necessarily of sufficient magnitude to overpower the enhanced dissipation. A full accounting of all the terms in the energy balance equation was necessary to determine the net effect of the presence of particles upon the stability of the flow.

Thickening of the Viscous Sublayer--Peskin and Dwyer; Lumley; Jotaki and Tomita

One of the earliest suggestions that drag reduction might be characterized by changes in the viscous sublayer was made by Peskin and Dwyer (Peskin and Dwyer 1964; Dwyer and Peskin 1965). They classified all two phase flows in terms of two parameters: (1) the ratio of the distance between the particles and the size of the smallest energy containing eddies and (2) the ratio of the particle size multiplied by the dispersed solids density and the size of the smallest energy containing eddy multiplied by the fluid density. Peskin and Dwyer suggested that simultaneous low (as compared to a packed bed or dilute systems of very large particles) values of these two ratios could produce viscous

disturbances in the sublayer. The viscous sublayer would be permitted to grow thereby reducing the wall shear stress.

Lumley (Lumley 1964) proposed that the mechanism for the known drag reduction caused by particles and fibers in suspension was the result of either one of two causes. The first was the introduction of additional length and time scales into the turbulent boundary layer and a consequent violation of Reynolds number similarity. The scales would probably be too small to affect the outer part of the flow but would be large enough to affect the near wall region and violate the law of the wall. The particles would increase the dissipation in the flow and oppose the unsteady motion of flow. However, Lumley discounted the description as damping the turbulence of the flow as observations of drag reducing liquid-solids suspension indicated increased turbulence intensities.

Lumley favored the second possible cause, interaction of the particles with the structure of the viscous sublayer. The thickness of the sublayer is indicative of the size of a disturbance that can be maintained at equilibrium without import of energy into the sublayer. Smaller disturbances will be damped by the viscous dissipation. The presence of particles would increase the dissipation and permit larger disturbances to stay in equilibrium and hence permit larger sublayer thicknesses. Using the law of the wake, Lumley derived an approximate form for the change in wall shear stress as a function of changes in the sublayer thickness for indefinitely large Reynolds numbers in a turbulent boundary layer. The equation indicated that thickening of the sublayer would reduce the wall shear stress. Provided the length and time scales introduced by the particles did

not affect the structure outside the sublayer, the von Karman constant would be unchanged and the logarithmic law would still be valid but would be displaced to a region further from the wall.

Jotaki and Tomita (Jotaki and Tomita 1971) have used the energy equilibrium hypothesis to formally determine the relationship between the flow properties and the thickness of the sublayer in the presence of particles. Using an assumed form for the velocity fluctuations in the viscous sublayer, the authors were able to close the hierarchy of the equations for the turbulent velocity correlations and to complete the integration of the energy equation. Because of the importance of this paper, the solution will be followed through in detail and expanded in Chapter 6 into a direct relationship between loading ratio and friction factor ratio for given particle sizes. A comparison with the results of the experimental work reported later in this thesis will also be given in Chapter 6.

NUSSELT NUMBER REDUCTION

A summary of the experimental evidence of Nusselt number reduction is shown in Table 2. The particle sizes are generally quite small although, as Boothroyd points out, the proper particle size for turbulence interaction may be a function of the pipe diameter and Reynolds number as evidenced by the time scale factor. Most experimental heat transfer, like pressure drop work, is confined to smaller pipe sizes and lower Reynolds numbers by physical restrictions. Therefore, the implication that Nusselt number reduction will not occur in larger pipes and at higher Reynolds

Table 2
Experimental Evidence of Nusselt Number Reduction

Reference	Solids Diameter and Type	Pipe Diameter and Orientation	Loading Ratio	Reynolds Number
Farbar et.al. 1957	10-210μ silica-alumina	0.69" vertical	0-0.20	13,500
			0-0.40	19,000
			0-0.70	23,000- 27,000
Tien et.al. 1962	30μ glass	0.71" vertical	0-2.5	15,000
			0-3.5	30,000
	30μ lead		0-3.0	15,000
			0-3.5	30,000
	200μ glass		0-3.5	15,000
			0-3.5	30,000
	200μ lead		0-3.5	15,000
0-3.5		30,000		
Abel et.al. 1963	1-10μ graphite	0.89" vertical	0-5.0	22,000- 65,000
Depew et.al. 1963	30μ glass	0.71" vertical	0.5-3.0	13,500
			0.5-3.0	27,400
Farbar et.al. 1963	30μ glass	0.67" vertical	0-1.5	15,300
			0-1.7	26,500
	70μ glass		2-3.0	15,300
Jepson et.al. 1963	211-295μ sand	1.5" vertical	0-2.0	14,300
			0-4.0	21,450

Table 2 (continued)

Reference	Solids Diameter and Type	Pipe Diameter and Orientation	Loading Ratio	Reynolds Number
Jepson et.al. 1963 (cont.)	211-295 μ	1.5" vertical	0-6.0	28,600
	sand		0-11.0	42,900
	76-104 μ sand		0-5.1	14,300-
				28,600
	422-599 μ sand		0-10.1	28,600-
				71,500
	850-1200 μ sand		0-2.5	21,450-
				71,500
	1200-1680 μ millet seed		0-8.5	21,450-
				71,500
Garrett 1964	243 μ sand	3" vertical	0-3.75	23,000
	159 μ sand		0-5.5	30,700
Szekely et.al. 1966	150, 300, 600,	cyclone	0-3.0	---
	1200 μ sand			
	200 μ bronze			
	175 μ iron			
Sukomel et.al. 1967	65 μ graphite	0.30" horizontal	0-8.0	15,000
Boothroyd et.al. 1970	0-40 μ zinc	1" vertical	0-20.	35,000
			0-8.0	53,000
			0-8.0	80,000
			0-5.0	100,000

Table 2 (continued)

Reference	Solids Diameter and Type	Pipe Diameter and Orientation	Loading Ratio	Reynolds Number
Bothroyd et.al. 1970 (cont.)	0-40 μ zinc	2" vertical	0-3.5	35,000
			0-3.0	53,000
			0-4.0	80,000
			0-2.5	100,000
		3" vertical	0-2.0	35,000
			0-2.0	53,000
			0-3.5	80,000
Depew et.al. 1970	30 μ glass	0.71" horizontal	0-4.5	10,000
			0-7.5	15,000
			0-4.0	30,000
	200 μ glass		0-6.0	15,000
			0-5.0	30,000

numbers is not necessarily valid. For those cases where deposition was not a problem, the consensus of most investigators was that the Nusselt number reduction, was a consequence of some form of interference with the turbulent structure of the gas.

Only Boothroyd postulated a detailed theory for the cause of the phenomenon. Boothroyd and Haque (Boothroyd and Haque 1970) performed separate friction factor and heat transfer tests in the same test equipment and found a remarkable resemblance between the two results. Vertically flowing suspensions of zinc dust, 0-40 μ diameter, were heated in 1, 2, and 3 inch bore vertical tubes at air Reynolds numbers between 35,000 and 100,000. The presence of solids suppressed the convective heat transfer coefficient almost the same amount as the friction factor at low loading ratios. At high loading ratios the increase in Nusselt number, if any, was much less than the increase in friction factor. In some cases distinct minima, between the loading ratios of 1.0 and 2.5, were achieved. The amount of Nusselt number reduction was most severe for the 1 inch pipe and correspondingly less severe for the 2 inch and 3 inch pipes. The divergence of the friction factor ratio and Nusselt number ratio at high loading ratios indicated that a quantitative type of Reynolds analogy between the two phenomenon would be difficult to determine. These results were correlated qualitatively with the magnitude of the time scale factor:

$$N_{T1} = \rho D^2 / \text{Re}_g \quad (7)$$

which was a reduced form of the time scale factor defined for

the friction factor tests but simplified to account for the uniformity of the particle type and size distribution. Boothroyd and Haque also defined a second time scale factor:

$$N_{T2} = \frac{\rho D^2}{\rho_p a^2 Re_g^2} \quad (8)$$

that would be more suitable near the wall. However, the results were not correlated with this second time scale factor.

From the correlations with N_{T1} , Boothroyd and Haque concluded that the Nusselt number ratio was a strong function of the first time scale ratio and loading ratio and only a weak function of Reynolds number. The correlation of the Nusselt number ratio with the time scale factor lend further credence to the contention that the presence of particles interferes with the turbulent structure of the fluid.

The authors defined the Nusselt number in terms of the difference between the measured wall temperatures and the calculated (from an enthalpy balance) mixed mean temperatures. By direct measurement of the solids temperature, the fluid temperature was found to be almost the same as the mixed mean temperature. Because the heat transfer medium at the wall was more likely the gas than the solids, a definition of the Nusselt number in terms of fluid properties alone was suggested. The equivalence of the mixed mean temperature (particularly with small particles) and the fluid temperature would require the Nusselt number based on either temperature difference to be the same. The experimental measurements of solids temperatures by Boothroyd and Haque, therefore, tended to refute the argument (Wilkinson and Norman 1967) that the dip in Nusselt number ratio at low loading ratios was a

result of the Nusselt number definition. Even using a pessimistic estimate of the temperature difference between the two phases, Boothroyd and Haque found that the two definitions of Nusselt number agreed within 10 per cent for their data.

The authors pointed out that the quantitative differences (trends were similar) between results of several earlier investigators could be attributed to (besides differences in particle and pipe diameters) quantitatively unaccountable factors such as electrostatic charging, particle agglomerations, and radiation. Boothroyd felt that corrections for radiative heat transfer would usually be negligible because of the opacity of the suspension, particularly near the walls where the particle concentration and agglomerating tendency of the suspension seemed high. The effect of any radiation heat transfer correction would be to reduce the calculated amount of convective heat transfer to the suspension.

In another paper (Haque and Boothroyd 1969) the authors suggested the existence of a crude Reynolds analogy between the turbulent radial momentum flux and the turbulent radial heat flux of a suspension. However, the forms of the flux components were stated to bear little resemblance to the known forms of the flux components in a single phase fluid because of the effect of the presence of particles on the fluid turbulence. The friction factor ratio was expressed as;

$$\frac{f_s}{f_g} \approx \frac{\rho_{ds} \overline{v'_{ds} u'_{ds}} + \rho \overline{v'_f u'_f}}{\rho \overline{v'_g u'_g}} \quad (9)$$

and the analogous Nusselt number ratio:

$$\frac{Nu_s}{Nu_g} \approx \frac{\rho_{ds} C_{pp} \overline{v'_{ds} T'_{ds}} + \rho C_{pg} \overline{v'_f T'_f}}{\rho C_{pg} \overline{v'_g T'_g}} \quad (10)$$

No quantitative relationship between corresponding suspending fluid or dispersed solids fluxes was provided.

Chapter 3

LABORATORY EQUIPMENT, PROCEDURES, AND TECHNIQUES

Because gas-solids drag reduction is a multi-faceted phenomenon, a basic investigation of its characteristics requires a wide variety of experimental equipment. In this study, instruments and apparatus were drawn from the diverse experimental aspects of fluid mechanics, heat transfer, electronics, optics, and solids handling. The essential feature of the experimental equipment was the basic closed loop system. With the exception of an open loop system used during a brief mass flow meter calibration, a closed loop system was used throughout the experimental program. The system design was flexible enough to allow changeover from one phase of the program to another with only minor modifications.

The closed loop eliminated two serious impediments to laboratory work with gas-solids suspensions. The first obstacle was the health hazard associated with excessive inhalation of the dusty exhaust from an open loop. This is a particularly serious problem when working with small particle sizes or large proportions of fines; carcinoma of the lung is linked with deposition of sub-micron particles deep in the respiratory tract (Owen 1969:425). The second obstacle was the expense incurred by the large solids inventory needed for open loop operation. Handling a large inventory requires complicated solids feed and retrieval

systems and often requires excessive laboratory space to contain bulky gravity feed and cyclone recovery devices.

This chapter describes the details and operational procedures of the basic closed loop system, the open loop calibration system, and the three modified systems, used during the three major divisions of the work. Several techniques, that were used successfully to operate equipment that is normally incompatible with a gas-solids environment, are also described.

BASIC CLOSED LOOP SYSTEM

The basic closed loop system is shown in Figure 1. The loop could have been described as a 12 foot high, 18½ foot long rectangle generally constructed of 1 inch outside diameter (O.D.), 0.065 inch wall, type 304 stainless steel tubing. The motive force for flow was provided by a centrifugal circulator which was capable of pumping gas and solids simultaneously.

Circulator and Circulator Controls

This investigation represented the third use of the circulator in studies of gas-solids suspensions. The two previous studies were conducted at The Franklin Institute (Wachtell and Waggener 1964) and The City University of New York (Rossetti 1969). Complete details and photographs of the circulator components can be found in The Franklin Institute report (Wachtell and Waggener 1964:25-31). The special circulator design and fabrication by The Franklin Institute was a consequence of its experience that standard centrifugal blowers were too delicate for suspension tests in a closed loop system (Wachtell and Waggener 1964:14-23).

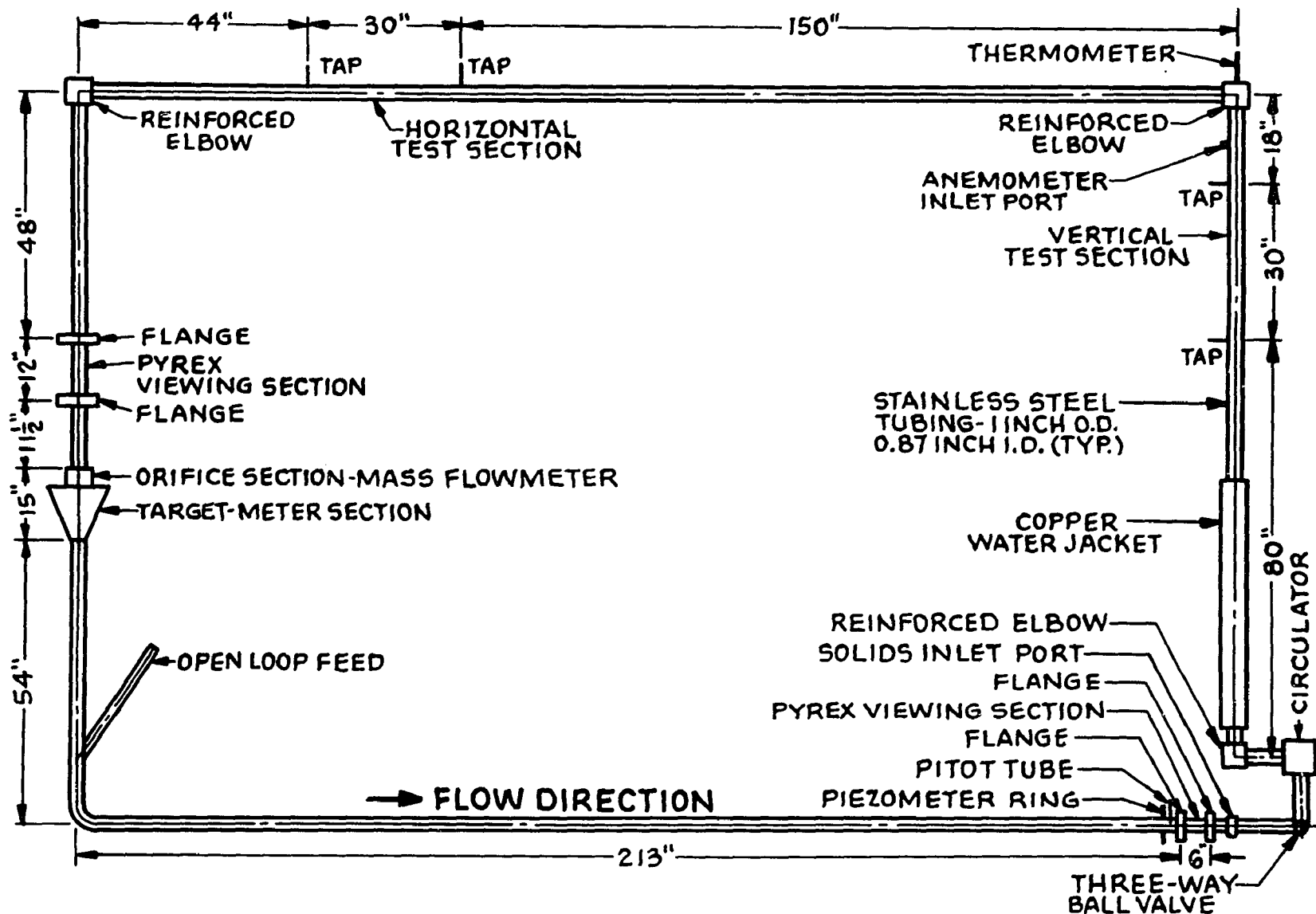


Figure 1
Basic Closed Loop System

The flow pattern inside the circulator could have been compared to that of a jet impinging perpendicularly upon the center of a rotating disk. The inlet tube aimed the suspension at a centered nose-cone on the rotor, which uniformly spread the suspension down channels cut into the rotor body. The flow discharged radially from the rotor channels into the rotor housing and out the exhaust tube. The important features of the construction included stainless steel components, all machined surfaces, and tight clearances between moving and stationary parts. The design was intended to avoid particle attrition, component erosion, and bearing abrasion.

In terms of the design objectives, the circulator performed well. Particle attrition was serious only for the largest particle size considered. There was some slight erosion around the periphery of the circulator rotor housing and on the nose-cone but none elsewhere. Abrasion of the replaceable bearings was a problem; however, it was not serious enough to warrant repairs during any of the major phases of the program.

The circulator was originally provided and operated with external cooling coils, a gas purge system for the bearings, and thermocouple assemblies for the measurement and control of bearing, housing, and shaft temperatures. These items were not used, except for the chromel-alumel thermocouples, which recorded bearing and housing temperatures during heat transfer runs. The circulator temperatures never exceeded 120°F, well below the 180°F maximum permitted by Wachtell and Waggener with an operating gas purge. Despite the tight clearances in the design, fine particles reached the bearings through the unused gas purge system; the gradual wear led to

bearing replacement at the end of this study.

The Franklin Institute design provided a 15 horsepower, special design slave motor, to drive the circulator at shaft speeds ranging from 1,600 to 13,200 revolutions per minute (r.p.m.). The slave motor was powered by a 25 horsepower, adjustable frequency generator. During the Franklin Institute study, panelboard mounted controls permitted operation of the circulator in two continuous shaft speed ranges between 1,600 and 6,600 r.p.m. and between 2,640 and 13,200 r.p.m. A panelboard mounted tachometer, graduated in percent maximum r.p.m., allowed setting the circulator shaft speed to within 150 r.p.m. of the desired value. The identical driving and control systems used in the two previous studies were also used in this investigation.

After almost ten years of intermittent operation, the circulator operated almost as efficiently as it did during the original Franklin Institute study. The ranges of circulator shaft speeds decreased slightly to 1,300 to 6,500 r.p.m. and 2,600 to 12,700 r.p.m. corresponding to panelboard tachometer ranges of 11 to 48 percent and 21 to 92 percent respectively. (The tachometer was calibrated against the circulator shaft speed with the aid of a General Radio Company model 1531-A stroboscope.) Under the heaviest solids loading, the slave motor was still capable of driving the circulator at shaft speeds corresponding to a panelboard tachometer reading of 88 percent.

Test Section and Upper Loop Details

The pump pulled suction from below and discharged into a short horizontal section. The flow was turned upward by a reinforced elbow, fabricated from 2 inch diameter, stainless steel barstock, milled to seat 1 inch O.D., 0.065

inch wall, stainless steel tubing. The flow traveled upward through 77 inches of stainless steel tubing, a Swagelok fitting, and another 3 inches of stainless steel tubing before encountering the first pressure tap in the vertical test section. The Swagelok fittings in the loop permitted sectioning of the system without introducing any disturbances in the approach to test sections and instruments. The 80 inch entrance length to the vertical test section provided an entrance length to diameter (L/D) ratio of 92 which was considered sufficient to guarantee fully developed flow for disturbed entry (Hinze 1959:514). A 48 inch long, 2 inch O.D., copper, water cooling jacket was provided 6 inches above the elbow to maintain a uniform loop temperature between 70 and 80°F during flow measurements. The jacket was connected to a water faucet in the laboratory with $\frac{1}{4}$ inch I.D. Tygon tubing. Before installation of the cooling jacket, mechanical heating by the pump and convection off the exposed tubing caused a 20°F temperature difference between opposite ends of the loop.

The vertical test section was 30 inches long and fabricated, like the entrance region, from 1 inch O.D., 0.065 inch wall, type 304 stainless steel tubing. The pressure taps, at either end of the 30 inch test section were fabricated from 1/16 inch O.D., 0.020 inch inside diameter (I.D.) stainless steel tubing and seated to match 0.020 inch holes in the wall of the test section. After soldering the taps to the test section, the tap holes were lightly polished to eliminate burrs. The lower tap was connected to one side of a 15 inch U-tube manometer, reading to 0.1 inch, with 1/16 inch I.D. Tygon tubing. The other side of the mercury manometer here and at the horizontal test section was open to

atmospheric pressure.

Three inches above the upper tap, the flow passed through another Swagelok fitting into a vertical 15 inch section containing the anemometer probe inlet located three inches above the Swagelok fitting and 12 inches below another reinforced elbow. Four different braces were installed to isolate this first vertical portion of the loop, particularly the anemometer probe location, from the vibration of the pump. An ASTM A17, mercury-in-glass thermometer was inserted into the reinforced elbow at the top of the vertical section. The thermometer had 0.2°F scale divisions in a total 68 to 80° thermometer range.

The flow turned horizontal at the reinforced elbow, encountering a Swagelok fitting 19 inches from the elbow, then passed through 119 inches of unbroken 1 inch O.D., 0.065 inch wall, stainless steel tubing, another Swagelok fitting and another 12 inches before reaching the first pressure tap in the horizontal test section. The 150 inch entrance length to the horizontal test section provided an entrance L/D ratio of 173 thus guaranteeing fully developed flow at the first tap.

The horizontal test section was 30 inches long and fabricated from 1 inch O.D., 0.065 inch wall, type 304 stainless steel tubing and was, therefore, identical to the vertical test section, except for orientation. The pressure taps and tap holes were identical to the pressure taps and tap holes in the vertical test section. The first tap was also connected to a 15 inch mercury U-tube manometer, reading to 0.1 inch, with 1/16 inch I.D. tubing. The taps were located at the top of the test section to avoid plugging by particle deposition.

The flow traveled horizontally from the second tap for 12 inches, passed through a Swagelok fitting, and then traveled another 32 inches before being turned downward into an unbroken 48 inch length of stainless steel tubing. Here the flow expanded into a 12 inch long, 1 inch I.D., pyrex viewing section. The pyrex-steel junctions were formed with standard glass-to-steel flanges. Since the inside diameters of the two sections were different, a smooth transition was provided by tapering the I.D. of the stainless steel tubing to its 1 inch O.D. and fitting to the tapered end of the pyrex pipe.

At the end of the pyrex viewing section, the flow reentered stainless steel tubing, encountering a Swagelok fitting after 4 inches, a thermocouple inlet port after another $4\frac{1}{2}$ inches, and the entrance to the mass flowmeter after still another 3 inches.

Mass Flowmeter

A cross section of the mass flowmeter is shown in Figure 2. The mass flowmeter was originally designed and built by the Bureau of Mines (Gibson, Abel, Fasching 1963). The construction was modified for use during two studies at the Morgantown Coal Research Center (Rosenecker, Coates, and Lucas 1966; Bluman, Galli, Coates, Spencer, and Rosenecker 1967). The final modifications were made at The City University of New York (Rossetti 1969). The identical mass flowmeter as modified by Rossetti (Rossetti 1969:70-72) was used throughout this study.

The meter was 15 inches long and contained two integral units. The first, a 4 inch long block, enclosed a 0.435 inch I.D. sharp-edged orifice. The second, an 11 inch long cone, contained a $\frac{1}{2}$ inch diameter, cantilevered steel target

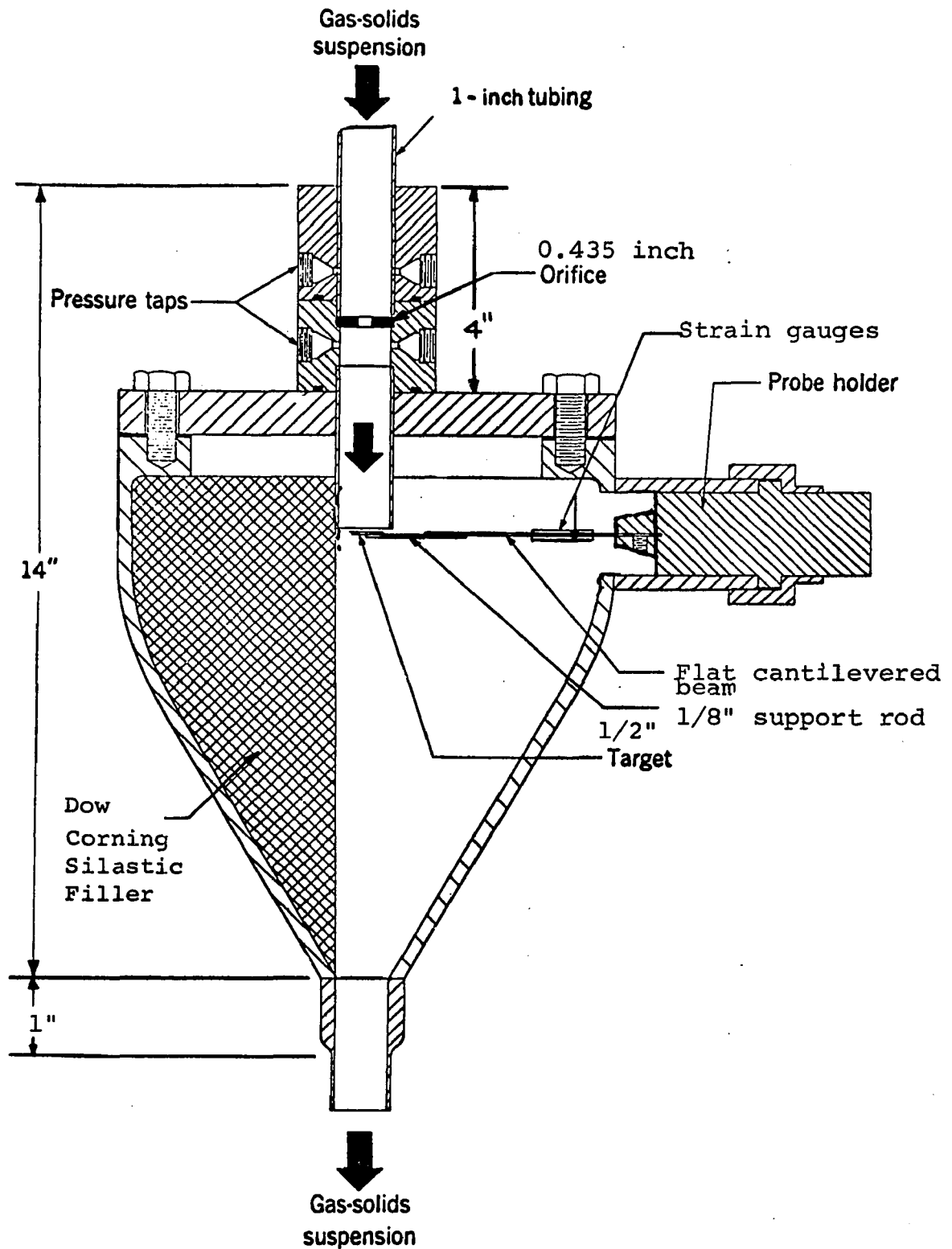


Figure 2
Mass Flowmeter

joined to a flat steel support by a 1/8 inch diameter, steel support rod. Temperature compensated strain gauges were mounted on the top and bottom sides of the flat steel support.

The strain gauges and two fixed resistors formed a Wheatstone bridge. A potentiometer was used for initial balance of the system and set permanently thereafter. The electrical signal from the bridge, indicative of the target deflection, was filtered and preamplified before being recorded on the roll chart of a Mark 280 Brush recorder. The filter provided a root mean square average of all signal fluctuations of less than 1.4 seconds duration. The (Clevite-Brush) preamplifiers were required to permit detection of the bridge signal which was as small as 1 microvolt. Two scales, 10 microvolts per division and 20 microvolts per division, were used to graduate the 50 division, Brush recorder, chart paper. Signal magnitude was, therefore, estimated to either 1 microvolt or 2 microvolts within the 0 to 800 microvolt range seen during the experimental program.

The orifice was located at acceptable distances from upstream and downstream flow disturbances (Perry 1963:5-12). The orifice pressure drop was read on a 31 inch differential water manometer with minimum divisions of 0.1 inch. The orifice taps were connected to the manometer by 1/4 inch I.D. Tygon tubing.

Lower Loop and Pitot-Static Section Details

After leaving the mass flowmeter, the flow passed through a Swagelok fitting, a 33 inch length of stainless steel tubing, another Swagelok fitting, and an 8½ inch section containing a plugged anemometer probe inlet and a

Y-connection used during open loop operation. Following the Y-connection, the flow traveled $12\frac{1}{2}$ inches before turning horizontal at an unreinforced elbow. No significant erosion was observed at this unreinforced elbow or anywhere else in the loop despite prolonged solids circulation.

In the lower horizontal leg, the flow traveled 203 inches, passing a Swagelok fitting 20 inches from the unreinforced elbow, before encountering another Swagelok fitting at the entrance of the 10 inch long pitot-static section. The pitot-static section contained a static pressure tap 2 inches from the Swagelok fitting and a centerline pitot tube and an eight tap piezometer ring $5\frac{1}{2}$ inches further downstream. The taps and tap holes were identical to the ones in the test sections. The pitot tube was constructed of 1/16 inch O.D., 0.020 inch I.D. stainless steel tubing and located in the centerline by a manual traverse manufactured by the United Sensor and Control Corporation.

The pitot tube was connected to the high pressure side of an R. Feuss, model 134b, micromanometer filled with 0.8 specific gravity alcohol. The low pressure side of the manometer was connected, through a manifold, to any one of the eight static pressure taps of the piezometer ring or to the separate static tap. The pressure leads were lengths of 1/16 inch I.D. Tygon tubing. After serving to verify the orifice coefficient, the pitot tube and static taps were not used because of recurrent plugging in suspension flow.

Following the pitot-static section, the flow expanded and contracted through a flanged 6 inch pyrex viewing section and entered a $7\frac{1}{2}$ inch section containing the solids inlet port. Except when particles were being added to the system, the port was closed with a machined plug matching the I.D.

of the stainless steel tubing. At the end of the 7½ inch section, the flow traveled through a Swagelok fitting, another 10 inches and encountered a 1 inch, three-way, ball valve. The ball valve either diverted the flow upward and toward the circulator intake pipe or allowed straight passage into a 36 inch long, 2 inch O.D., sintered tube.

During loop cleanouts, the three-way valve was turned for straight passage; and, a 1 inch ball valve located in a tee above was opened. This scheme permitted the pump to suck in clean room air and exhaust particle-laden air into the sintered tube. Otherwise the flow was diverted upward passing the closed tee 2 inches above, and returned to the circulator intake 5 inches above the tee.

The entire loop was grounded to avoid accumulation of static electric charges during suspension flow experiments. Cables were attached across glass sections to provide a continuous charge leakage path to the common ground. The loop was bubble-tested for pressure leaks under 100 pounds per square inch pressure provided by an externally supplied compressed air line. All leaks were sealed before performing any of the data collection runs.

Procedures

Two procedures were common to all phases of the experimental program. They were warmup of the mass flowmeter and startup of the circulator. The combination of the filter, preamplifier, and Brush recorder required a two hour warmup before the pen trace on the roll chart stabilized at a constant value. The circulator did not require a warmup but was never started before the mass flowmeter had stabilized and been adjusted for no flow conditions. Detailed

operating instructions for the warmup of the mass flowmeter and the startup of the circulator are listed in Appendix A.

OPEN LOOP CALIBRATION SYSTEM

The response of the strain gauge mounted target-meter was dependent on both the gas flow rate and the solids flow rate. The orifice provided an independent measure of the gas flow rate, even in suspension flow, and therefore was used to determine the response of the target-meter to the flow of gas alone. A weigh-meter technique, requiring an open-loop operation, was used to calibrate the target-meter for suspension flow. The independent effect of the solids alone upon the target-meter response was determined by subtraction of the known effect of the gas flow. Once calibrated in the open loop, the target-meter was used to measure the solids flow rate in the closed loop system.

The open loop calibration system provided facilities for continuous feed and collection of directly measurable amounts of solids. A permanently installed section of the basic closed loop system became the feed section of the open loop calibration system simply by opening one valve. Some modifications were required to convert another section of the basic closed loop system to the collection section of the open loop calibration system. With the exception of the feed and collection sections, the two systems were identical. Figure 3 shows the important features of the feed and collection sections.

Feed Section

A BIF Industries model 25-02 solids feeder and Prater model AV-60 rotary airlock combination was permanently

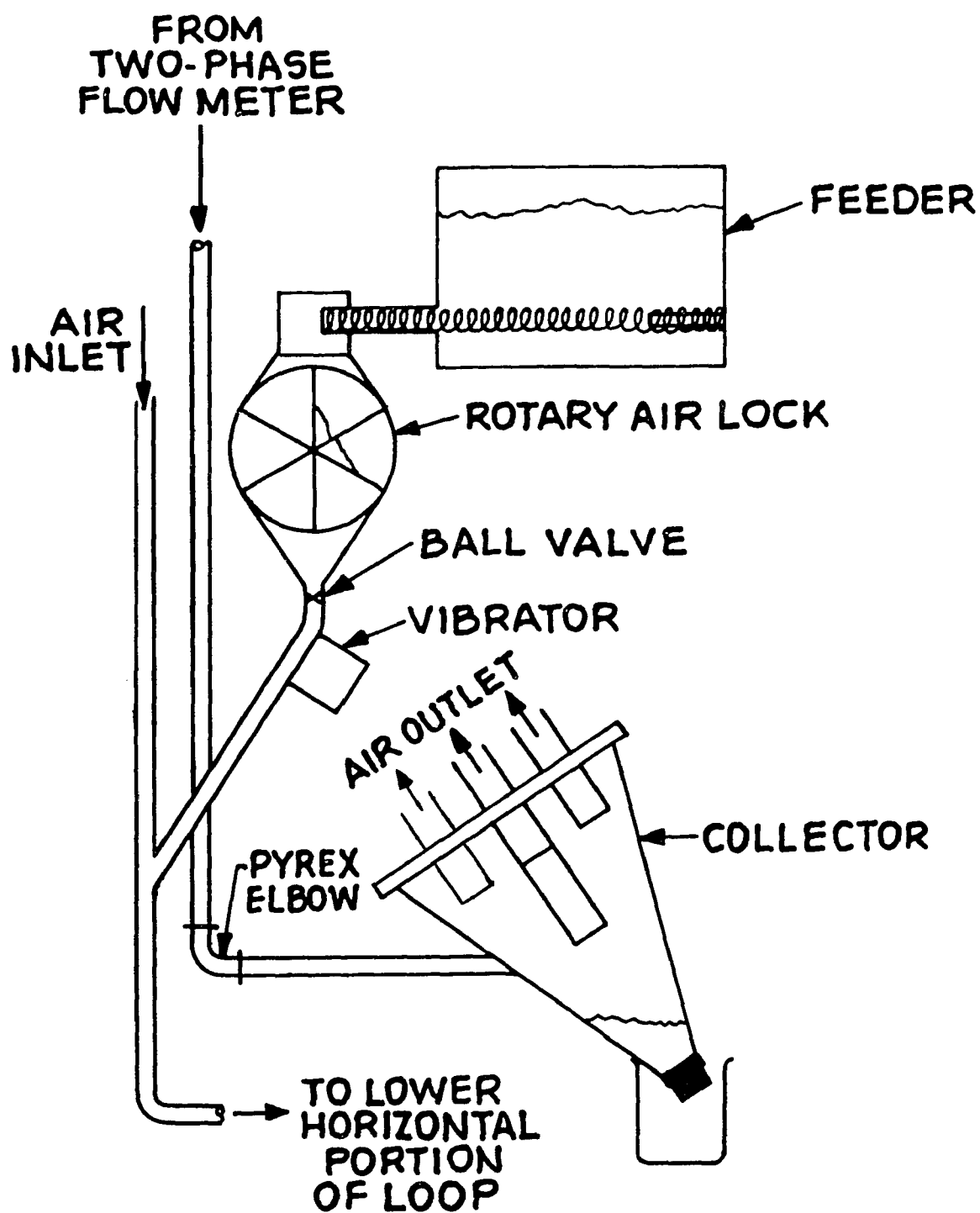


Figure 3
Feed and Collection Sections

installed above the Y-connection, found in the vertical downflow portion of the loop. The feeder discharged solids from the bottom of a hopper with the aid of interchangeable $3/4$ inch or $1\frac{1}{2}$ inch helical feed screws. The feed rate was varied between 0 and 15 grams per second by changing the setting of the eccentric crank connected to the feeder chain drive unit. The discharge from the feeder fell into the six compartments of the rotary air lock and then into the top of the 26 inch skewed branch of the Y-connection. A one-inch ball valve below the outlet of the air lock controlled the passage of the solids into the loop. (During the closed loop operations, this ball valve was always closed.) The (Cleveland Vibrator Company) vibrator located beneath the ball valve prevented solids plugging.

Collection Section

The 33 inch length of stainless steel tubing below the mass flowmeter was removed and replaced with a 29 inch length of stainless steel tubing leading to a flanged 4 inch radius pyrex elbow, and the specially designed particle collector. In this arrangement, room air was sucked in through the tubing above the Y-connection, mixed with the solids at the Y-connection, and passed once through the lower loop, circulator, upper loop and mass flowmeter before reaching the particle collector.

The particle collector was a modification of the design used by Rossetti in an earlier investigation (Rossetti 1969: 74-76). The collector represented a compromise between the efficiency of a cyclone and the safety of a sintered tube. The inlet pipe and body shape resembled that of a cyclone; the multiple outlet pipes were fabricated from capped

sintered tubes. The previous design forced the particle laden air to exhaust through three 2 inch O.D. sintered tubes. Two more sintered tubes, making a total of five, were installed during this investigation in order to reduce the pressure drop through the collector and increase the carrying gas flow rate. The cyclone-sintered tube combination performed quite well as most of the solids fell to the bottom of the cyclone; negligible amounts of solids collected on the sintered tubes. During calibration runs, the gradual accumulation of solids on the sintered tubes reduced the gas flow rate; however, the decrease in the gas flow rate never exceeded 10 percent during any one run.

Procedures

Before a calibration run, an empty beaker used to collect the solids from the bottom of the cyclone and a rubber stopper used to seal the bottom of the cyclone were weighed on a triple beam balance reading to 0.1 gram. Then the eccentric crank was set to the desired value; an electric timer, reading to 0.1 second, was set to zero; and, the circulator was run up to maximum speed. At the start of the actual calibration run, the ball valve was opened and the feeder, airlock, timer, and vibrator were started simultaneously. During the run, the thermometer, orifice water manometer, horizontal section mercury manometer, and strain gauge readings were recorded.

After a given time interval, between 15 and 300 seconds depending on the solids feed rate, the ball valve was closed and all equipment was shut down simultaneously. The stopper was removed from the cyclone; and, the cyclone contents and stopper fell into the beaker. Particles adhering to the sides of the cyclone were loosened by rapping the sides of

the collector. The beaker, stopper, and solids were weighed to determine the weight of solids collected; and, after division by the duration of the run, the average solids flow rate was obtained.

After each run, the loop, cyclone, and sintered tubes were cleaned and prepared for the next run. At least fifteen runs were performed for each of three different particle sizes. Switching to a calibration run with a different particle size or just switching feed screws required emptying and cleaning the hopper and airlock in order to avoid biasing a subsequent run in favor of a previous one. To avoid biasing during a series of runs with the same particle size and the same feed screw, random, as opposed to uniformly increased, crank settings were selected for individual runs.

PRESSURE DROP MEASUREMENT SYSTEM

The first major phase of the experimental program was the measurement of pressure drops for the purpose of calculating friction factors in both clean gas and suspension flow. The basic closed loop system was used for these measurements. However, additional equipment was installed to measure the pressure drops across the test sections and to verify the value of the orifice coefficient.

Equipment

Two R. Feuss model 134b, micromanometers filled with 0.8 specific gravity alcohol were used to measure the pressure drops across the vertical and horizontal test sections. The micromanometer tubes could be set at any one of five graduated angles or equivalent scale factors, but were

usually set at the 0.4 scale factor and were, therefore, read to minimum scale divisions of 0.4 millimeters of water. The micromanometer leads were taken directly from the taps to the high and low pressure sides of the micromanometer with 1/16 inch I.D. Tygon tubing. Initially, the micromanometers were connected to the taps through a manifold; however, this scheme proved impractical as manifold leaks and the large inertia of the air in the long lead lines and manifold caused serious errors and experimental difficulties.

A DISA S&B constant temperature anemometer, operated in conjunction with a thermistor probe located above the vertical test section, was used briefly during pressure drop measurements in order to verify the gas flow rate calculated from the orifice manometer reading. The probe was positioned at the tube centerline by a DISA S&B traverse. Details of the anemometry will be given in the section of this chapter entitled "Profile Measurement System".

Procedures

With the exception of two items, Reynolds number selection and particle addition, the experimental technique for pressure drop measurement was identical for clean gas and suspension flows. The Reynolds numbers of the clean gas runs were varied by changing the setting of the panelboard tachometer, in increments of 5 percent of maximum r.p.m., in ascending order from 35 to 90 percent and then in descending order from 90 to 35 percent. The Reynolds numbers of the suspension runs were set at approximately 12,000; 14,000; 16,000; 18,000; 20,000; and 22,000 by adjusting the circulator speed until the orifice manometer reading corresponded to one of these six values. Solids were added to the closed

loop, between sets of suspension runs, by pouring weighed and sifted amounts, in 10 gram increments, into the solids inlet port near the pitot-static section. For a particular amount of solids in the closed loop, pressure drop measurements were taken at all six Reynolds numbers, in decreasing order, before shutting down the circulator and adding more particles. At heavy solids loadings, runs at Reynolds numbers of 22,000 and 20,000 often were not possible because of limitations on the circulator speed. Suspension runs were terminated if significant loss of entrainment was observed in the horizontal pyrex viewing section. After a full day's operation, the loop was cleaned out so that fresh particles could be circulated during the next day's operation. Occasionally a compressed air line was used to blow out accumulated dust from parts of the closed loop.

An individual run involved setting the circulator speed to the desired value and taking thermometer, vertical test section micromanometer and mercury manometer, horizontal test section micromanometer and mercury manometer, orifice water manometer, and strain gauge recorder readings. An example of the special data sheet prepared for flow measurements is given in Appendix B. During some initial runs which included calibration checks, pitot-static micromanometer and anemometer direct current (D.C.) voltmeter readings were taken. Long warmups between runs were not required as the mass flowmeter output, after the day's initial two hour warmup, stabilized at a constant value within a few minutes after setting the circulator speed.

PROFILE MEASUREMENT SYSTEM

The second major phase of the experimental program was the measurement of gas mean velocity, gas turbulent intensity, gas turbulent spectral, and particle velocity profiles. The gas profiles were obtained in both clean gas and suspension flow and provided information on the effect of the presence of particles. The basic closed loop system and electronic anemometry was used for the gas profile measurements. The particle velocity profile measurements required one modification to the loop and the addition of some photographic equipment and accessories.

Gas Profile Measurements

All gas profile measurements were made with electronic anemometry and probes manufactured by the DISA S&B Corporation. The basic anemometer unit consisted of a Type 55D01 constant temperature anemometer, a type 55D25 auxiliary unit, a type 55D30 D.C. voltmeter, and a 55D35 root mean square (r.m.s.) voltmeter. Four different types of anemometer probes were tested or used: thermistor, conical wedge film, hot wire, and fiber-film.

Anemometer circuitry. Figure 4a shows the standard operating condition of the Wheatstone bridge incorporated in the constant temperature anemometer. The variable resistor was used to select the probe operating resistance and overheat ratio. The probe was maintained at constant temperature and resistance by a feedback circuit to a servo amplifier. The bridge output, after some filtering, amplification, and zero adjustment through the anemometer and auxiliary unit was read out on the voltmeter to 0.01 volt. This voltage was a measure of the cooling affect and the mean velocity of the

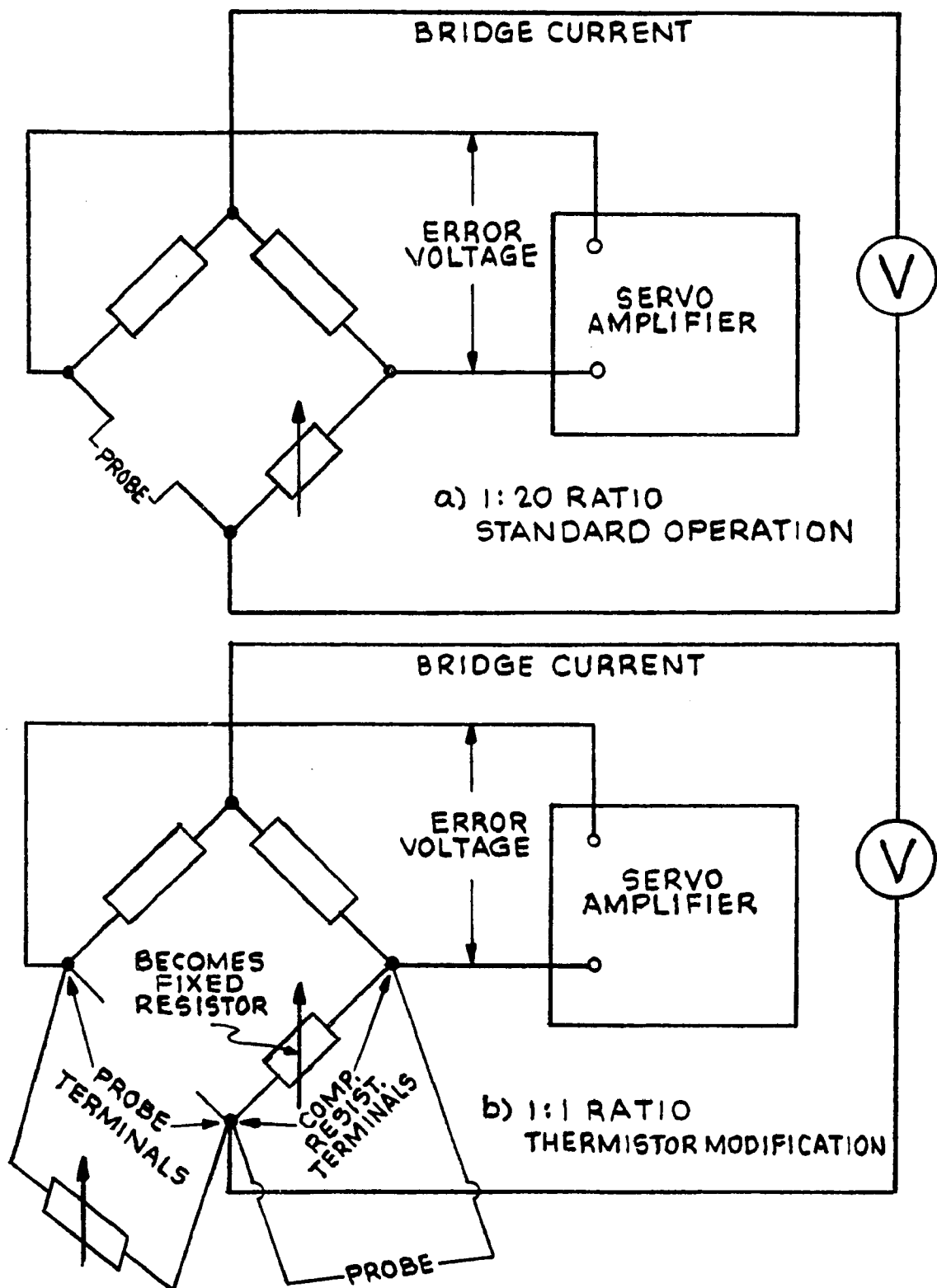


Figure 4
Anemometer Circuitry

fluid passing over the probe sensor. The r.m.s. voltage, read out on the r.m.s. voltmeter to 0.0001 volt, was a measure of the magnitude of the fluctuating components of the velocity passing over the probe sensor. The standard operating condition was used for the conical wedge film, hot wire, and fiber-film probes.

Use of high resistance (approximately 60 ohms) thermistor probes required modification of the standard operating conditions, as shown in Figure 4b. The bridge ratio, the impedance ratio between the active and passive bridge arms, was changed from 1:20 to 1:1 and an external, non-inductive, resistor was connected. The negative slope of the resistance-temperature relationship of the thermistor probe necessitated connection of the probe to the compensating resistance terminals and connection of the external resistor to the probe terminals of the anemometer. The feedback system in the unit was polarized to operate with the positive slope, temperature-resistance relationship, of the most common probes. A thermistor probe, therefore, presented the possibility of unstable and destructive oscillations in the feedback system unless the probe and external resistor connections were reversed.

Probes and probe traverse mechanism. The sensor of the thermistor probe was embedded inside a 540 μ diameter glass bead and withstood the bombardment of the particles extremely well. However, the large probe size destroyed the sensitivity of the probe to the fine grain structure of turbulent flow. Consequently it recorded very low levels of turbulence even in a highly turbulent stream.

Another drawback of the thermistor probe was its extreme sensitivity to the temperatures of the flow. The probe had

to be calibrated for every degree Fahrenheit change in the gas temperature and proved to be more sensitive to temperature changes than to velocity changes. To insure accurate mean velocity measurements, the ambient temperature had to be measured accurately to 0.1°F . Because of these difficulties, the probe was used only for some preliminary calibration work.

The ceramic coated conical wedge film probe was rugged enough to withstand the bombardment of 10 to 60μ particles but was not used for any extended period because of its large size relative to the tube I.D. The large probe disturbed the flow pattern and did not yield a true description of the undisturbed flow.

The standard 5μ hot wire was, as expected, found to have the most sensitivity to turbulence. However, it failed to remain intact longer than ten seconds in a suspension of 30μ particles. Therefore, it was completely unsuitable for measurements in the suspension. It was used in clean gas service only for comparison with the results obtained with other probe types and with different overheat ratios.

The fiber-film probe was similar in geometry to the hot wire probe but was much more robust. A 2μ thick metallic film was sputtered onto a cylindrical 70μ diameter, 1.25 mm sensitive length, glass fiber and coated with a 2μ thick layer of abrasion resistant quartz. The probe was only slightly temperature sensitive in a 15°F range and registered about 60 to 70 percent of the turbulence registered by the hot wire. The probe failed after several hours use in a dilute suspension of 30μ particles because of the loss of the quartz coating. The failure appeared in the form of constantly varying resistance of the film as measured on the

anemometer. However, the probe life was sufficient to obtain meaningful measurements of gas mean velocity, turbulence intensity, and turbulent spectral profiles for a wide variety of flow conditions.

The probes were positioned at various radial locations in the tube using a DISA S&B type 55H01 traversing mechanism. The traverse was manually gear-operated to an accuracy of 0.01 mm. The anemometer probe inlet was sealed against leaks during traverse movement by a $\frac{1}{4}$ inch Swagelok fitting containing teflon bushings. The entire mechanism was mounted on a specially made steel support that was rigidly affixed to the outside of the vertical tube.

Spectrum analyzer and voltage regulator. The spectrum analysis was performed by taking the signal from the output of the r.m.s. voltmeter and sending it to the input of a Tektronix type 1L5 spectrum analyzer plugged into a Tektronix type 535A oscilloscope. The spectrum analyzer was operated with a vertical logarithmic amplitude display, covering a 6 decibel (over a 0.0005 base voltage) range. The horizontal display covered a linear 0 to 2,000 Hertz range with a 200 Hertz dispersion or bandwidth. In order to eliminate errors caused by the excessive electrical noise level in the laboratory power lines, all electronic equipment was connected to the secondary of a 1000 watt Sola constant voltage transformer. Before the installation of the regulator and the common grounding to the regulator, the r.m.s. voltmeter reading caused by noise was almost as high as the signal caused by turbulent flow. The spectrum analyzer indicated the noise was occurring at the line frequency of 60 Hertz and at even multiples of the line frequency. After installation, the noise level registered by

the r.m.s. voltmeter was two orders of magnitude smaller than the signal produced by the turbulent flow.

Probe calibration procedures. All probes were calibrated inside the cylindrical windtunnel of the DISA type 55D41/42 calibration equipment. The windtunnel provided a flat, low turbulence, velocity profile at the probe location. The velocity at the probe location was calculated from the pressure difference measured between the entrance and exit of the windtunnel nozzle leading to the centrally mounted probe. All probes were calibrated at the same angular orientation used during actual profile measurements. An ASTM A17 thermometer was located at the nozzle entrance to monitor ambient air temperature. The probe calibrations usually covered at 0 to 100 feet per second velocity range and a 70 to 80°F ambient temperature range. Typical calibration curves are given in Chapter 4 of this dissertation.

Traverse locations. A thermistor probe was used to locate the traverse position corresponding to the inside wall of the tube. The traverse setting where the tip of the probe sensor coincided with the outside edge of the anemometer probe inlet port was noted. Then the gear traverse was turned until the probe touched the far inside wall of the tube, thereby determining the distance from the outside edge of the probe inlet port to the tube wall. By subtracting the tube radial dimension from this distance, the location of the tube centerline was determined relative to the outside edge of the probe inlet port. Therefore, an external reference point was located for all traverse locations.

The 10 points chosen for the traverse locations corresponded to 0, 22.4, 38.7, 50.0, 59.2, 67.1, 74.2, 80.6,

86.6, and 92.2 percent of the distance from the tube centerline to the inside tube wall. These values except for the centerline position were calculated from a standard formula (Perry 1963: 5-7) for a 20 point full diameter traverse. Because the probe support would strike the seal on a traverse toward the near inside wall, traverse locations were restricted to the far side of the tube diameter. The standard formula calculation suggested that an additional point be located at 97.5 percent of the radial dimension. However, interaction between the flow around the large sensor supports and flow past the nearby wall would have rendered the results meaningless. None of the probes, with the possible exception of the hot wire, would have yielded a realistic representation of the undisturbed flow this close to the wall. Unfortunately the hot wire probe was too fragile for suspension measurements.

Measurement procedures. The procedure for gas profile measurements was identical for both clean gas and suspension flow. After warming up the electronic equipment for two hours, a calibrated probe was positioned at the centerline of the tube and the probe inlet port was sealed. The circulator was set to the desired speed and thermometer, horizontal section mercury manometer, orifice water manometer, and strain gauge readings were recorded. Occasionally test section micromanometer readings were taken to spot-check pressure drop measurements in suspension flow.

The probes were moved through the traverse locations while the circulator was running. At each location, the D.C. voltage, r.m.s. voltage, and spectral display were recorded. Because the hot wire and fiber-film probes were relatively insensitive to the variations in the gas or suspension

temperature, the effect of slight temperature variations could be eliminated by constantly checking the probe operating resistance and adjusting the variable resistor to maintain a constant overheat ratio. The variation in probe operating resistance remained slight, unless the probe was physically damaged, because the water cooling jacket maintained the temperature between the narrow range of 70 to 80^oF. However, the thermistor probe was extremely sensitive to even slight temperature variations, therefore, the fluid temperature had to be recorded for every measurement taken with the thermistor probe. It was not possible to adjust the overheat ratio in the modified operating condition required by the thermistor probe.

Particle contamination had the effect of uniformly shifting the fiber-film probe calibration curves. The probe rapidly accumulated a fixed amount of particle cover. This was concluded from the observation that the D.C. voltmeter readings remained constant at a particular traverse location as long as the circulator speed and the overheat ratio remained constant. In order to determine if the response of the fiber-film probes had been effected by contamination, the probes were calibrated before and after particle contamination. The calibration curves were different but exactly parallel to one another. This suggested that one known point on the calibration curve of an operating contaminated probe could be used to determine the entire curve. In fact this procedure was used to determine the mean gas velocity during profile measurements. The close coincidence between the centerline velocity calculated from the orifice manometer reading and the centerline velocity measured during preliminary calibration work by the thermistor probe,

which had not contaminated, provided the one known point in the form of the vertical centerline velocity.

The measurement of relative turbulence intensity was independent of probe contamination. Measurements of turbulence intensity profiles with clean or contaminated fiber-film probes gave identical results. Probe contamination apparently affected all voltage readings uniformly and, therefore, was a self-cancelling factor in the formula for relative turbulence intensity. The fiber-film probes were calibrated before and after every complete traverse with the value of the no-flow voltage and the slope of the voltage-velocity curve being noted. In all cases the calibration curves were parallel and relatively close to each other.

Preliminary measurements of clean gas profiles were made at panelboard tachometer speeds of 20, 30, 40, 50, 60, 70, 80, and 90 percent of maximum r.p.m. (roughly corresponding to a range of Reynolds numbers between 5,000 and 25,000) with the thermistor probe. The hot-wire probe was used to measure clean gas profiles at only two Reynolds numbers 12,000 and 22,000. The fiber-film probes were used to measure clean gas profiles at Reynolds numbers of 12,000; 14,000; 16,000; 18,000; 20,000; and 22,000 and suspension profiles at a variety of particle sizes, gas Reynolds numbers, and solids loading ratios. The suspension conditions were selected to be representative of the friction factor results.

Particle Velocity Profile Measurements

Particle velocity measurements were obtained photographically by the double streak method. The measured distance between two streaks on a photograph represented the

movement of a particle over a known time interval and, therefore determined the particle velocity.

Equipment. The vertical test section of the closed loop system was replaced with a flanged section of 1 inch I.D., clear pyrex pipe. The camera, a 35 mm single lens reflex Canon Pellex with a 32mm, F/4.5, Bausch and Lomb macrolens on a bellows extension, was mounted on a movable support adjacent to the pyrex pipe. The flash tube of a model 502, EG&G multiple microflash unit, was mounted directly opposite the camera lens. This arrangement produced shadow streaks rather than actual particle images.

The focused depth of field of the camera was extremely narrow, only 0.047 inches. The narrow depth of field was a result of the high magnification produced by the bellows-macrolens combination, 7.21 power on the 35mm negatives. The magnification and depth of field of the camera system was determined by photographing a metal scale in two different planes and measuring distances directly on the negative.

The microflash unit was set to flash twice in 0.0001 seconds. Because the camera shutter was not synchronized with the flash unit, the entire photographic system--flash tube, lens, pyrex pipe--was enclosed in a lightsealed cover and the shutter kept open continually. The only time the film was exposed was during the two brief, about 10 microsecond, flashes. The fastest commercially available black-and-white roll film, Kodak Tri-X pan rated at ASA 400 was used.

Procedures. The camera focal plane was located by establishing a focused reference position on the pyrex pipe O.D., measured at 1.317 inches. The traverse positions in the

pipe; 0, 25.0, 37.5, 50.0, 56.2, 62.6, 68.8, 81.2, 87.4, 93.8, and 100 percent of the radial dimension from the centerline, were located by moving the camera support to marked gauge positions. In this way, neither the camera magnification nor the camera focal length was changed.

Before taking photographs, the thermometer, horizontal section mercury manometer, orifice water manometer, and strain gauge readings were recorded. The camera traverse proceeded in the opposite direction as the anemometer traverses, that is, from the centerline toward the near wall rather than from the centerline to the far wall. Because of the short focal length, the camera lens would have struck the pyrex pipe before a traverse of the far side could have been completed.

HEAT TRANSFER MEASUREMENT SYSTEM

The third and final major phase of the experimental program was the measurement of heat transfer coefficients for clean air, air-glass suspensions, clean argon, argon-glass suspensions, and argon-aluminum suspensions. The basic closed loop system with several modifications was used for all heat transfer work. The heat transfer system was designed with two objectives in mind: efficient and accurate data collection and prevention of damage to temperature sensitive equipment. Figure 5 shows the modified closed loop system.

Test Sections and Equipment

Two primary test sections were provided, a heated vertical section and a cooled horizontal section. The vertical test section was similar to the vertical test section of .

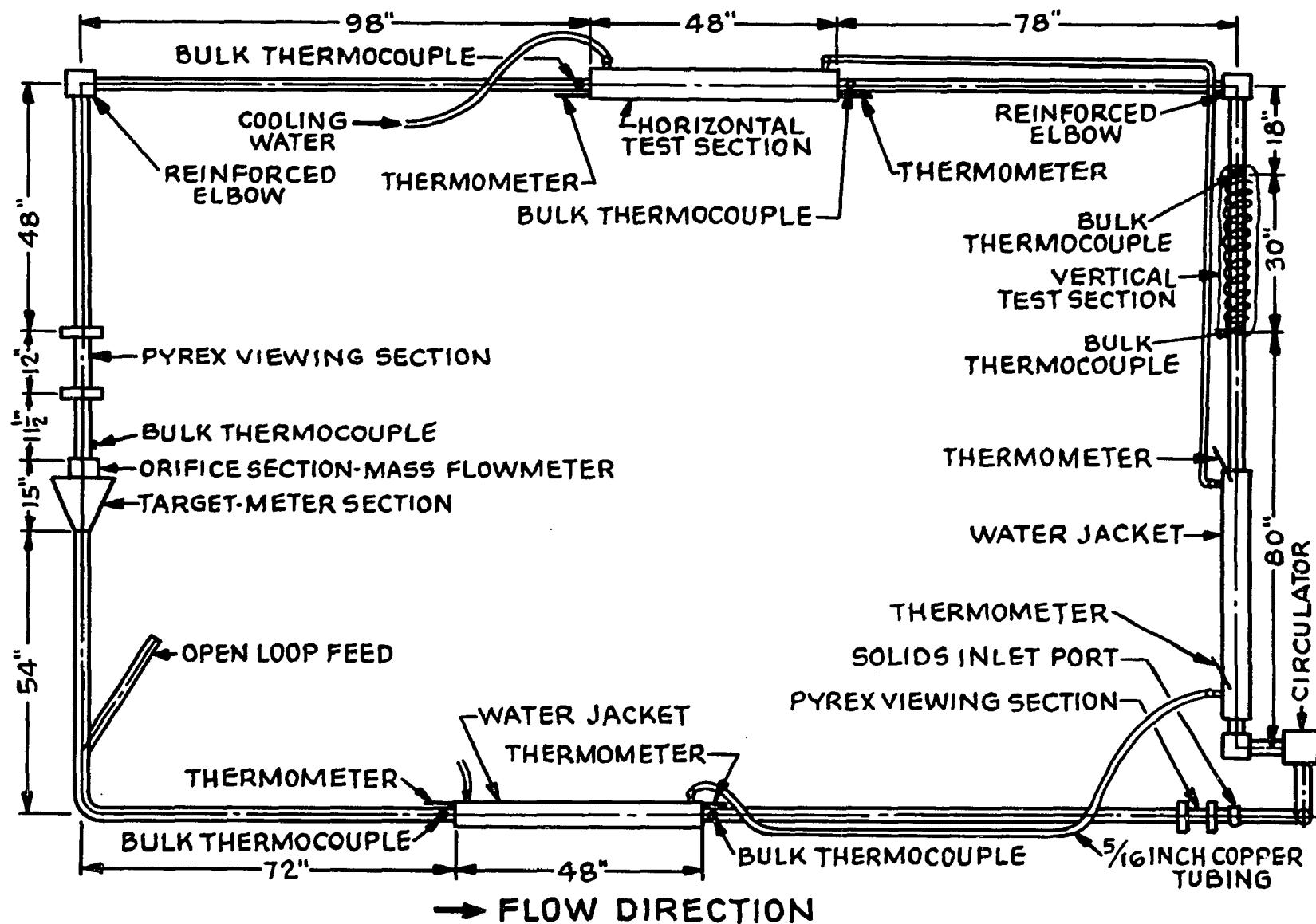


Figure 5
Closed Loop Modified for Heat Transfer

the pressure drop measurement system. It was fabricated from a 30 inch length of 1 inch O.D., 0.065 inch wall stainless steel tubing and located, as before, 92 tube diameters upstream of the reinforced elbow. Thermoelectric company miniature chromel-alumel thermocouples were located at either end of the test section and positioned to measure centerline bulk temperatures. The thermocouples were inserted through mounting bushings welded to the tube wall. The bushings seated securely on the 1/16 inch O.D. thermocouple sheath and did not permit leakage. Two diametrically opposed chromel-alumel thermocouple junctions were located every six inches along the test section to measure outside wall temperatures.

The heat input in the vertical test section was supplied by an electrical resistance tape wrapped around the tube. The tape was controlled by a 0 to 120 volt range Variac voltage regulator and supplied a maximum of 384 watts. Several inches of fiberglass and asbestos wool insulation were wrapped around the tape to minimize heat conduction to the atmosphere. A final layer of aluminum foil was added to minimize radiative heat losses.

A 48 inch long, 2 inch O.D., copper, water cooling jacket was located 78 inches downstream of the reinforced elbow and served to identify the horizontal test section for heat transfer. Chromel-alumel bulk thermocouples were located at either end of the test section to measure suspension temperatures. ASTM A9F thermometers, reading to 1⁰F, were inserted through nylon fittings in the water jacket to measure the inlet and outlet water temperatures. The water jacket here provided sufficient cooling to prevent overheating of the silastic filling and epoxy strain gauge coating

in the mass flowmeter further downstream. A chromel-alumel bulk thermocouple was located above the orifice to monitor the temperature of the fluid entering the mass flowmeter.

Another 48 inch long, 2 inch O.D., copper, water cooling jacket was located 72 inches from the unreinforced elbow in the lower horizontal section. Although this section was equipped with the same instrumentation as the upper horizontal section and was occasionally used as a test section, its main purpose was to protect the pump from excessive fluid temperatures. Five of the original chromel-alumel thermocouples on the circulator were used to monitor housing and bearing temperatures.

The three copper, water cooling jackets were connected in series by 5/16 inch O.D. copper tubing and formed three counterflow parallel pipe heat exchangers with the stainless steel closed loop tubing. A rotameter, reading to 0.02 gallons per minute (g.p.m.) monitored the 0 to 1 g.p.m. water flow rate from the laboratory sink.

All thermocouple readings were taken from a 24 point Leeds and Northrup model H multipoint dial recorder. The dial read to 5⁰F over an 800⁰F range but could be estimated to 1⁰F. All electrical equipment was commonly grounded through the 1000 watt Sola constant voltage transformer.

Procedures

Not less than a one hour warmup period with the circulator running preceded each set of data runs with a given amount of particles in the loop. The cooling water temperature and the maximum wall temperature in the vertical test section had to stabilize before reliable data could be taken. At the beginning of each set of data runs, the cooling water flow rate, the circular shaft speed, and the Variac setting

were set to the desired values. Because of limitations on the allowable tape temperature, the Variac setting was adjusted so that the maximum wall temperature in the vertical test section did not exceed 420°F .

The experimental plan for the sets of heat transfer runs was identical to the plan used for the sets of pressure drop runs. An individual run recorded 12 vertical wall, 7 bulk, 5 pump, and 6 water cooling temperatures; horizontal mercury manometer and orifice water manometer readings; rotameter and strain gauge readings; and the Variac setting. An example of the special data sheet prepared for heat transfer measurements is given in Appendix B. Although a one-hour warmup was required between sets of runs, only a fifteen minute period was required between individual runs where only the gas Reynolds number was varied. For argon gas measurements, the system was purged of air by forcing the denser argon into the lower loop and venting the lighter air from the upper loop.

Chapter 4

CALIBRATIONS AND CLEAN GAS RESULTS

Calibrations serve three important purposes: translation of original data, identification of equipment and material limitations, and definition of the reasonable precision and accuracy of a test. The first purpose is the obvious one and is the one that provides the numerical results. The last two establish the validity and the reliability of the conclusions drawn from the results. Because the experimental apparatus was subjected to harsh and unusual service conditions, during this study the last two purposes assumed equal importance with the first. Consequently, a large proportion of the experimental program was allotted to calibration and equipment testing. This chapter includes the detailed results of particle size analyses, mass flowmeter calibrations, and anemometer probe calibrations. The particle size analyses are included with the calibrations because they define the size limits of the solids before and after circulation.

Clean gas experiments could also be considered as calibrations because they provide comparison with accepted results. In this study the clean gas results were an intermediate step between the initial calibrations and the final suspension tests and were, therefore, used to verify the experimental technique. This chapter also includes the

results of clean gas pressure drop, profile, and heat transfer measurements.

PARTICLE SIZE ANALYSES

All particle size analyses were performed with an Industrial Model B Coulter Counter equipped with a 140 μ diameter aperture tube. The Coulter Counter drew a known volumetric sample of electrolytic solution, 500 microliters of Isoton, containing suspended particles through the aperture. Two electrodes on either side of the opening sensed a reduced current whenever a particle passed through the opening. The unit was adjusted to count the number of reductions and hence the number of particles larger than a pre-selected size. The results of several sampling tests defined the size distribution of a given particulate.

The calibration factor, 65.7 micron³, a measure of the unit sensitivity to particle size, was determined by initial calibration with mono-sized 19.5 μ ragweed pollen. The coincidence factor, 6.88, a measure of the number of simultaneous particle passages, was calculated from a formula in the Coulter Counter operating manual. The equivalent spherical diameter corresponding to the settings of the unit controls was also calculated from a formula given in the manual.

Five grades of spherical glass beads designated Superbrite numbers 980, 981, 279, 380, and 660 by the 3M Company were circulated during flow measurements. During heat transfer measurements only the #279 and #380 glass beads were circulated. One other particle grade, spherical aluminum powder designated grade H-322 by the Valley Metallurgical Processing Company, was circulated during heat transfer

measurements. Figures 6 through 11 show the size distributions of the six particle grades. The glass beads before circulation, with the exception of the #660 glass beads, had narrow distributions. The aluminum powder had a much broader distribution than any of the glass beads.

After circulation, the effects of particle degradation tended to broaden the distributions. The H-322 powder could not be sized after circulation because of the existence of large aluminum agglomerates which continually plugged the aperture of the Coulter Counter.

A summary of the particle size analyses is shown in Table 3 where weight mean and arithmetic mean diameters are given. The new (uncirculated) #980 designation was the smallest diameter by weight, 15.0μ , with the #981, #279, #380, #660, and H-322 designations being larger at 21.0, 21.6, 36.0, 55.0 and 30.0μ respectively. The diameters did not change significantly, after circulation, except for the diameter of the #660 glass beads which decreased almost three-fold. The slight increase in the diameter by weight of the used (after circulation) #279 glass beads was within the stated 10 percent precision limits of the Coulter Counter. With the noted exception of the #660 glass beads, the size order by weight remained unchanged after circulation. Considering the #980 and #279 glass beads as being the same size, the size order by number (arithmetic mean), again with the exception of the #660 glass beads, also remained unchanged.

The method of calculating the arithmetic mean diameter tended to bias the size toward the lower diameter range where aerodynamically insignificant bits of particulate debris were numerous. The method of calculating the weight mean diameter

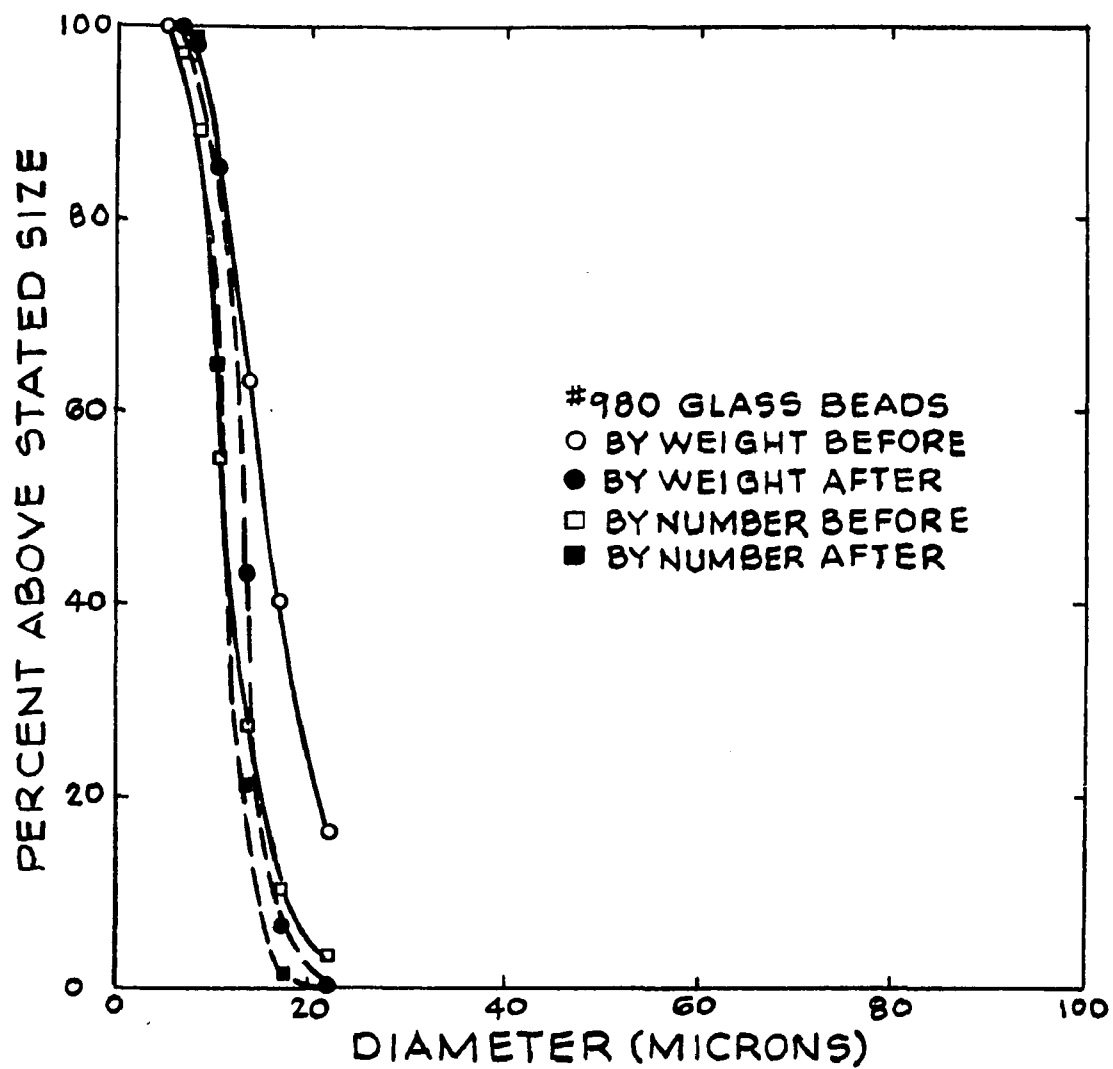


Figure 6
Size Distrubutions - #980 Glass Beads

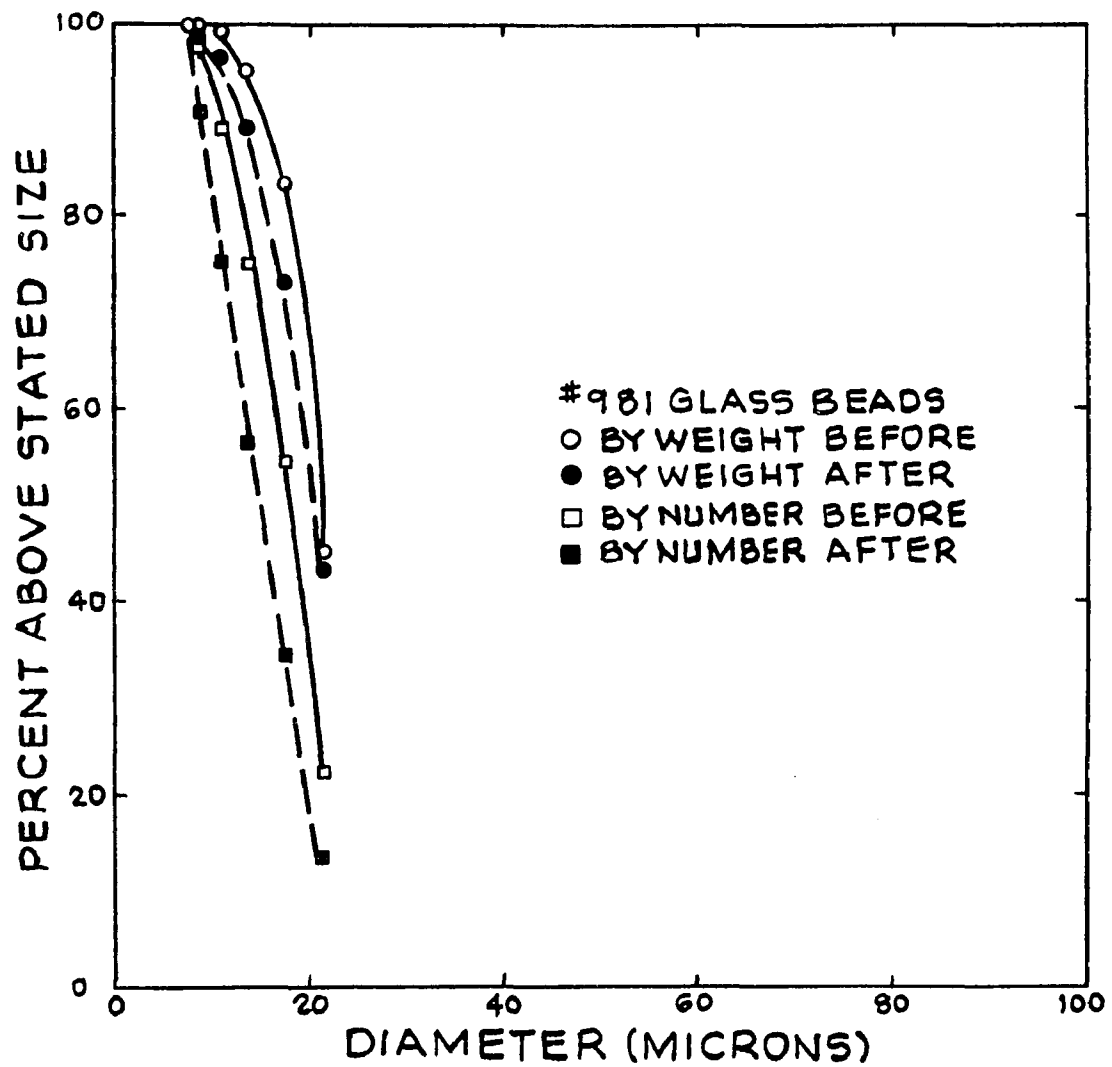


Figure 7
Size Distributions - #981 Glass Beads

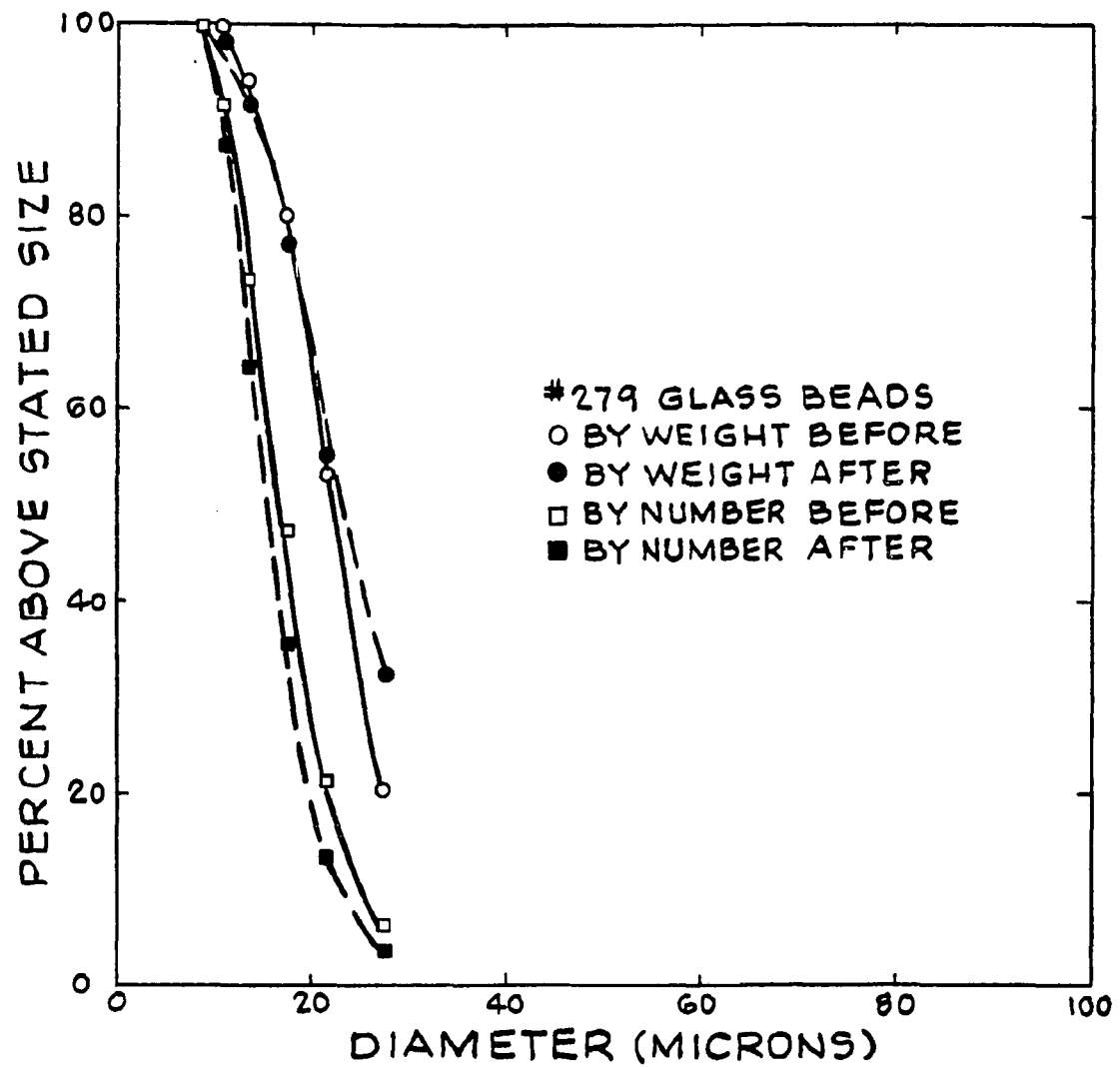


Figure 8

Size Distributions - #279 Glass Beads

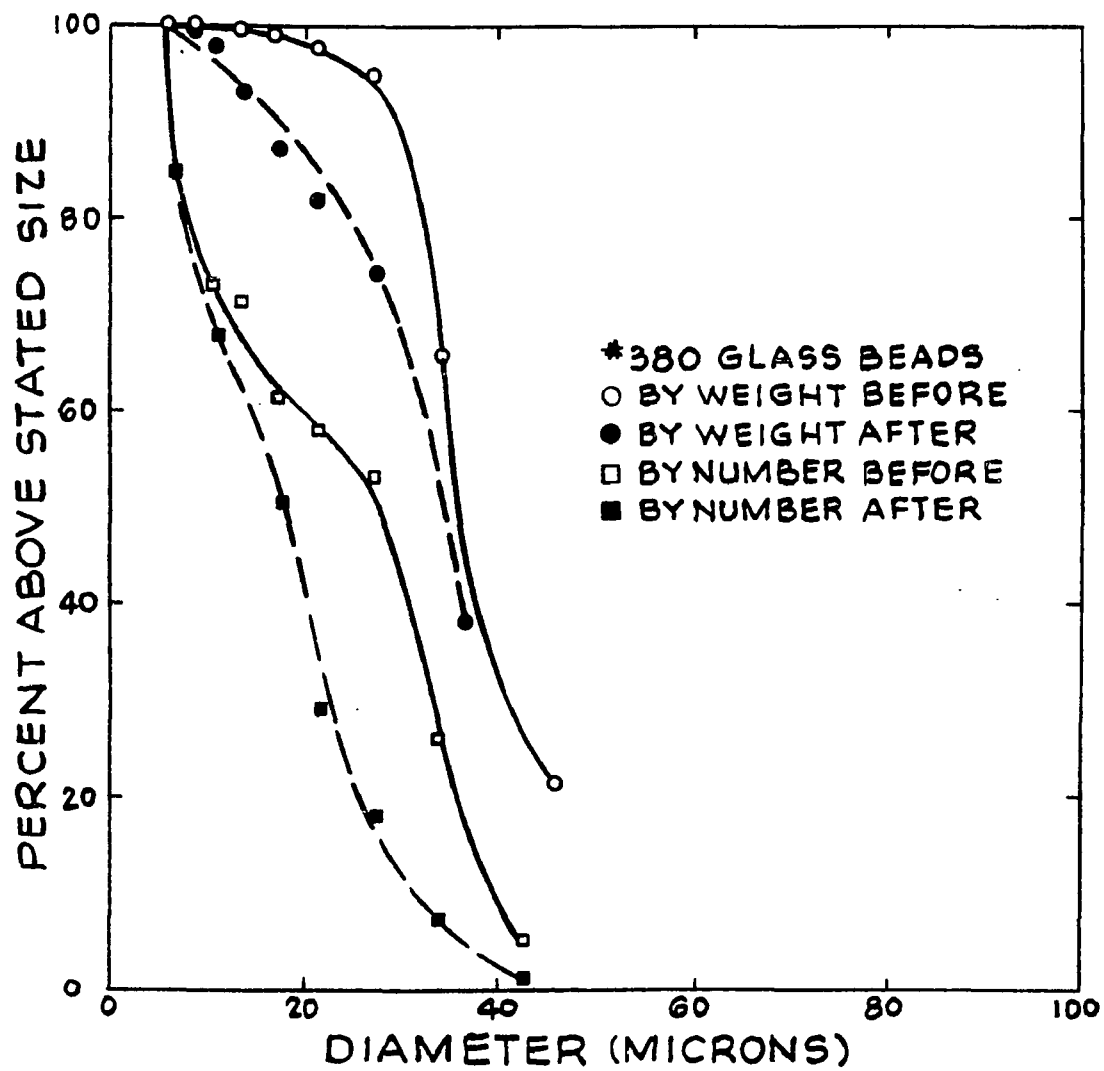


Figure 9

Size Distributions - #380 Glass Beads

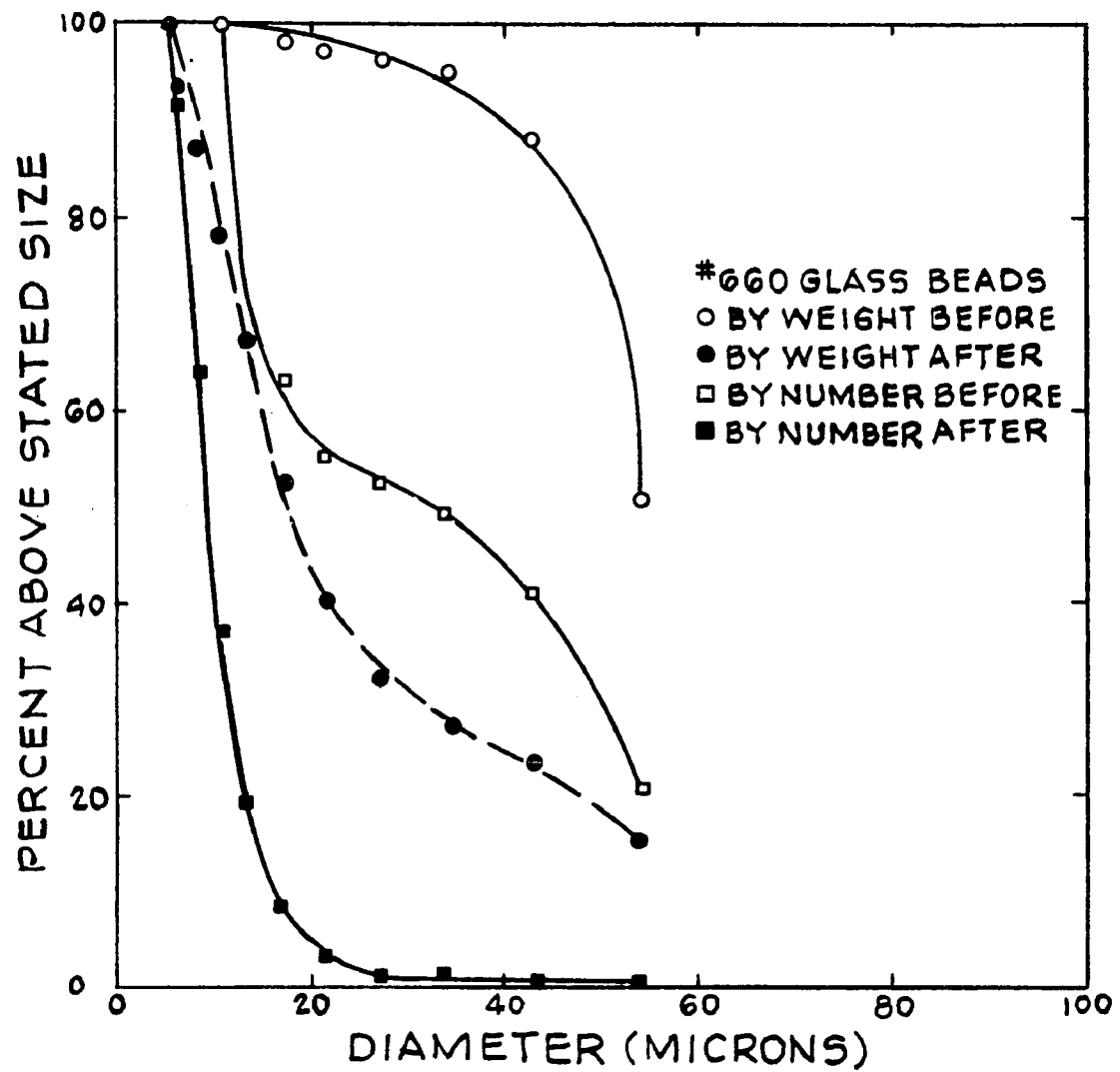


Figure 10

Size Distributions - #660 Glass Beads

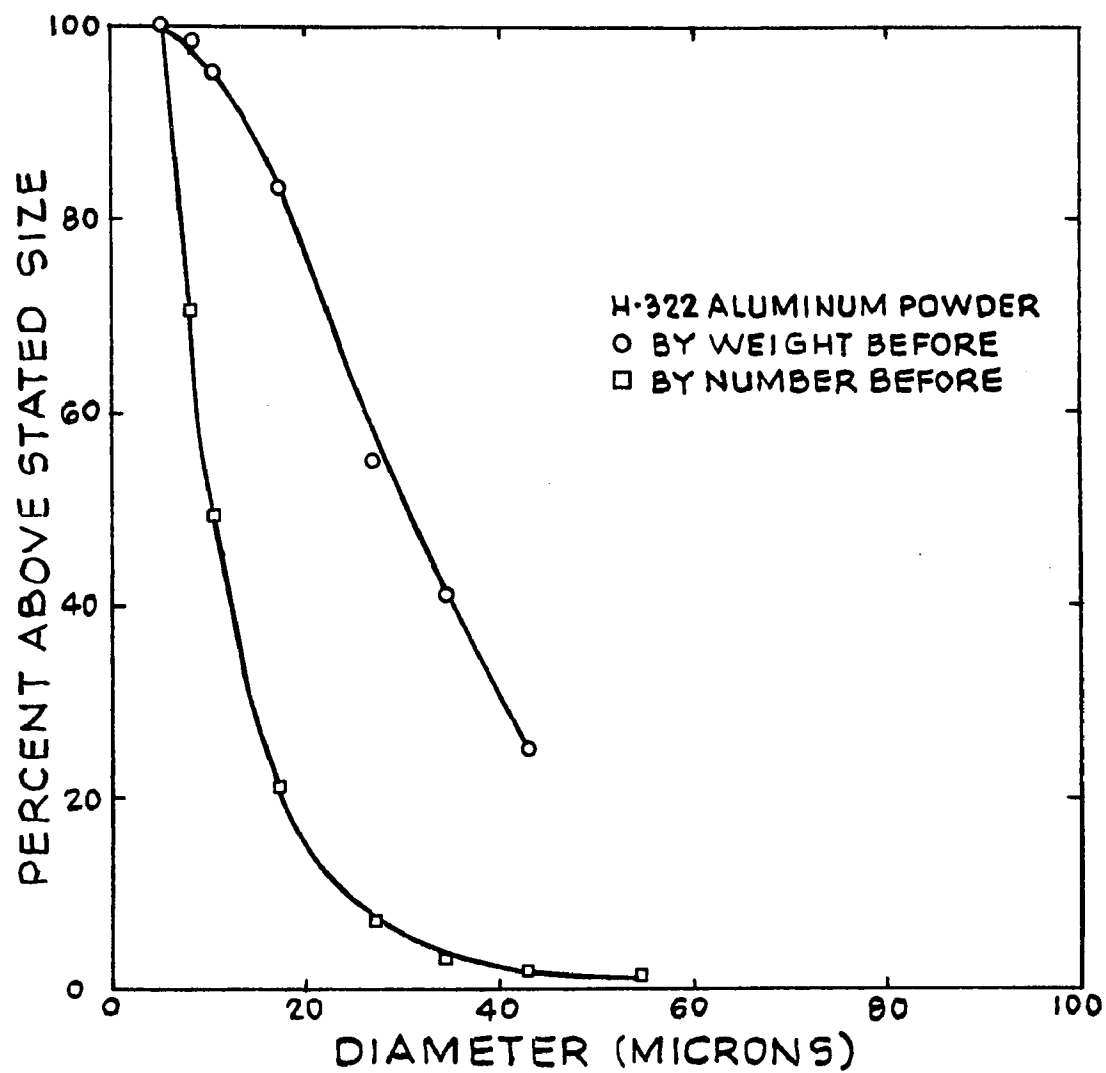


Figure 11
Size Distributions - H-322 Aluminum Powder

Table 3
Average Particle Diameters

Bead Designation	Weight Mean Diameter Before, After Circulation (microns)	Arithmetic Mean Diameter Before, After Circulation (microns)
#980	15.0, 13.1	11.0, 11.5
#981	21.0, 20.2	17.4, 14.4
#279	21.6, 22.4	16.7, 15.0
#380	36.0, 35.0	28.0, 17.6
#660	55.0, 18.0	34.0, 9.4
H-322	30.0, not sized	11.0, not sized

emphasized the larger and more aerodynamically significant particle sizes. The analysis of particle diameter dependent flow phenomena was accordingly based on the weight mean diameter.

MASS FLOWMETER CALIBRATIONS

The mass flowmeter contained two flow sensitive devices, a sharp edged orifice and a cantilevered target-meter. Both required extensive calibration as they were the primary metering elements for the entire experimental program.

Orifice

The value of the mass flow rate of the suspending gas was required independently of the suspension mass flow rate in order to calculate such quantities as the loading ratio and gas Reynolds number. Several investigators (Boothroyd 1971:69-70) have found that the presence of solids in suspension does not affect the characteristics of a sharp-edged orifice. This conclusion was confirmed during the course of experimental work.

The accepted equation (Perry 1963:5-8) for the mass flow rate through an orifice is:

$$W_g = C Y A_o \sqrt{\frac{2 g_c (P_1 - P_2) \rho_1}{1 - \beta^4}} \quad (11)$$

Based on the diameter of the orifice (0.435 in.), the diameter of the upstream tube (0.870 in.), and the location of the upstream and downstream pressure taps, standard references (Perry 1963:5-10) give an orifice coefficient of 0.612 for orifice Reynolds numbers (Reynolds numbers based on orifice diameter and velocity) greater than 30,000. This

value is very close to the value of the orifice coefficient found by a previous investigator (Rossetti 1969:101-102) using the identical orifice meter in suspension flow. Since the orifice Reynolds numbers for all experimental runs were either very near or exceeded 30,000, the orifice coefficient was taken as a constant of 0.612.

For incompressible flow, the expansion factor Y is unity. For gases, an approximate expression (Perry 1963: 5-10) for Y is:

$$Y = 1 - \frac{1-r}{K} (0.41 + 0.35\beta^4). \quad (12)$$

The value of the expansion factor was limited to a narrow range between 0.983 and unity by the maximum values of the exhaust pressure of the two phase pump. For the majority of the experimental runs, the expansion factor exceeded 0.990. Therefore, Y was taken to be unity throughout.

The density upstream of the orifice was calculated using the ideal gas law and temperature and total pressure measured as near to the orifice as possible. The temperature in the loop during friction factor and profile measurements was read from the ASTM A17 mercury in glass thermometer located above the anemometer probe and was assumed to be applicable at the orifice. During heat transfer measurements, the temperature was read from a chromel-alumel thermocouple located in the vertical section above the orifice. The total pressure was taken from the first pressure tap in the horizontal test section and measured on one of the mercury manometers. The total pressure here never exceeded 2 inches of mercury above atmospheric pressure obviating the need for the expansion factor as discussed above.

The mass flow rate measured by the orifice was compared to mass flow rates calculated from measurements by two other devices, a pitot tube and a thermistor anemometer probe, both located to measure the centerline velocity. For the considered range of Reynolds numbers based on average velocity and pipe inside diameter, 10,000 to 23,000, an equation given by Schlichting (Schlichting 1968:563-564) shows that the ratio of the mean velocity to maximum velocity in a smooth circular pipe will vary between 0.799 and 0.807 respectively. Consequently, the ratio of mean velocity to centerline velocity was taken as 0.800 in calculations of gas mass flow rate and gas Reynolds numbers. Comparison of mass flow rates measured by the three instruments for clean gas flow were in excellent agreement confirming the value of the orifice coefficient. Figure 12 shows this result for a wide range of circulator shaft speeds.

In suspension flow, the average flow rate calculated from the thermistor reading was in substantial agreement (within 1.8 percent) with the orifice flow rate provided the relationship between mean velocity and centerline velocity was unchanged. This relationship was confirmed for both suspension and clean gas flow by the velocity profile measurements made with the hot-film anemometer probe. (These measurements will be described in later sections.) Therefore, the orifice calibration for the clean gas flow was also valid for the gas flow containing suspended solids. Because of the reliability and accuracy of the orifice, it was used throughout the entire experimental investigation to measure the gas mass flow rate.

Target-Meter

Probably the most important parameter in this study was

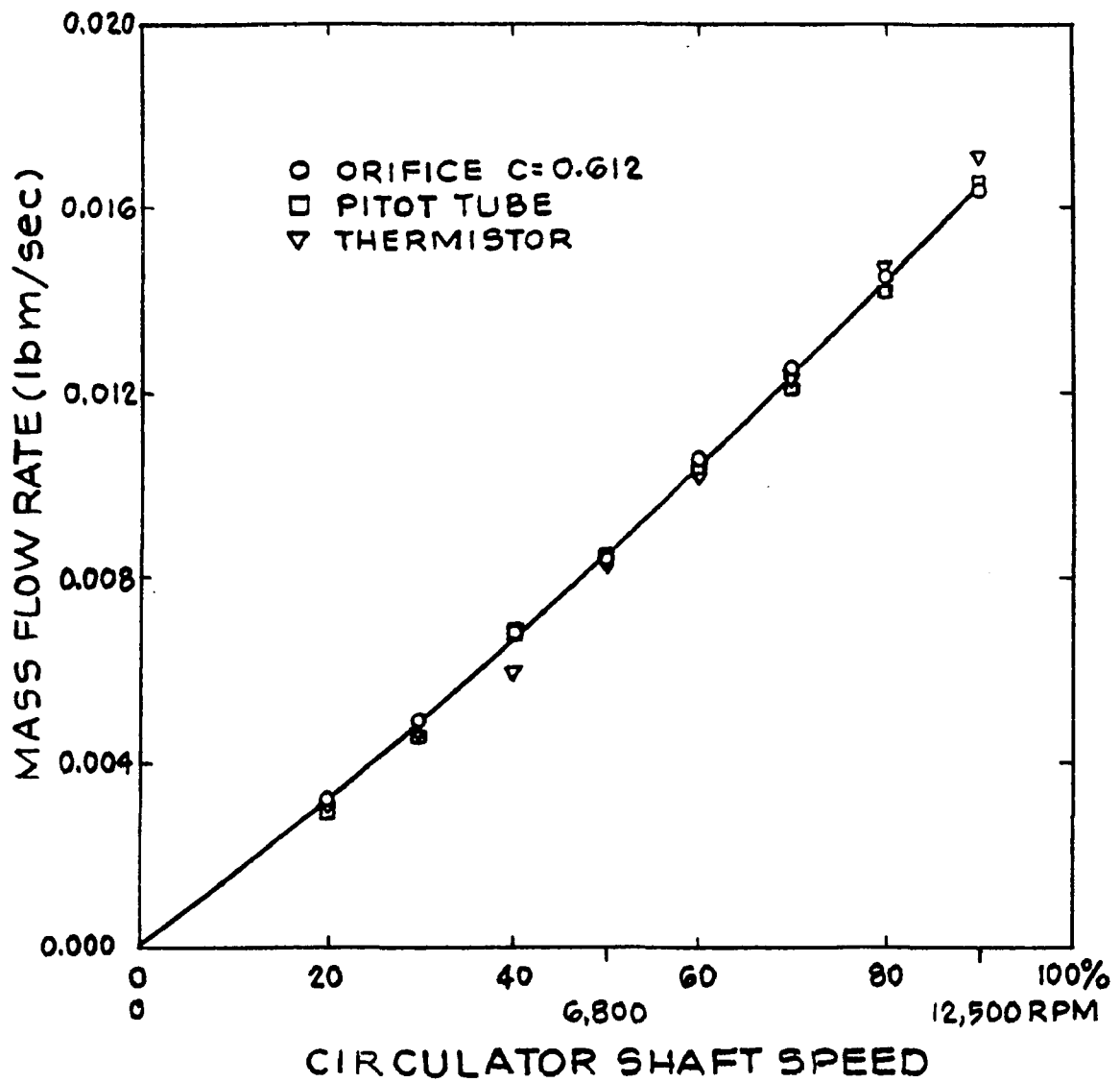


Figure 12
Clean Gas Flow Rate Versus Circulator Shaft Speed

the loading ratio, the ratio of solids mass flow rate to gas mass flow rate. This quantity is generally quite difficult to measure in a closed loop system since the dispersed solids density and average solids velocity are usually unknown. After calibration in the open loop system, the response of the target-meter and the velocity calculated from the orifice provided sufficient information to directly calculate the loading ratio in the closed loop system.

The operation of the target-meter was based on the linear relationship between the deflection of a cantilever and the force producing the deflection. The deflection of the cantilevered circular target was sensed by two temperature compensated strain gauges mounted on the target support rod. The force on the target was produced by the momentum change of the gas and solids striking the target.

Because the electronic processing and recording system did not impose any non-linear gain on the strain gauge signal, it was expected and confirmed that the recorder output was linear with the force acting on the target. The relationship between the recorder output and the force was not expected to depend on the nature of the medium, gas or solids, producing the force:

$$V_{ST} \propto F = F_g + F_p \quad (13)$$

For clean gas flow, the force acting on the target was calculated from:

$$F_g = C_D A_T \frac{\rho U^2}{2g_c} \propto V_{ST0}^2 \quad (14)$$

The target was a $\frac{1}{2}$ inch diameter disk supported by a $\frac{1}{8}$ inch diameter rod. The target was nominally located in the center

of the 0.870 inch diameter fluid stream. Ignoring the effect of the support rod, the drag coefficient of the target corresponded to that of a disk placed perpendicular to a confined flowing stream. For disk Reynolds numbers greater than 2000, the case here, the drag coefficient of a disk in an infinite stream is constant at 1.10 (Streeter 1962:206). Using the value of the disk drag coefficient in an infinite stream as an estimate for the coefficient in a confined stream, Figure 13 was prepared.

The plot of V_{STO} versus $C_{D,T} \rho U^2 / 2g_c$ shown in the figure revealed the excellent linear correlation between the two quantities. The slight deviation of the line from the origin was the result of the initial setting of the pen trace at a position one chart division -- the width of the pen trace -- away from the left-hand zero marker. The graph at once confirmed the constancy of the electronic system gain factor and the constancy of the disk drag coefficient. The figure also indicated that the proportionality factor between the recorder output voltage and the force acting on the target was also a constant. For the loading ratio calibrations (which are derived later) to be valid, the gain factor, the disk drag coefficient, and the proportionality factor had to be constant although their individual values were not required.

The force of particles striking the target was estimated with reasoning borrowed from the kinetic theory of gases. Consider one particle striking the target normally and rebounding elastically. Its momentum change would be $2mV_p$. The momentum change per unit time of a swarm of particles would equal the product of the momentum change of one particle multiplied by the number of impacts per unit

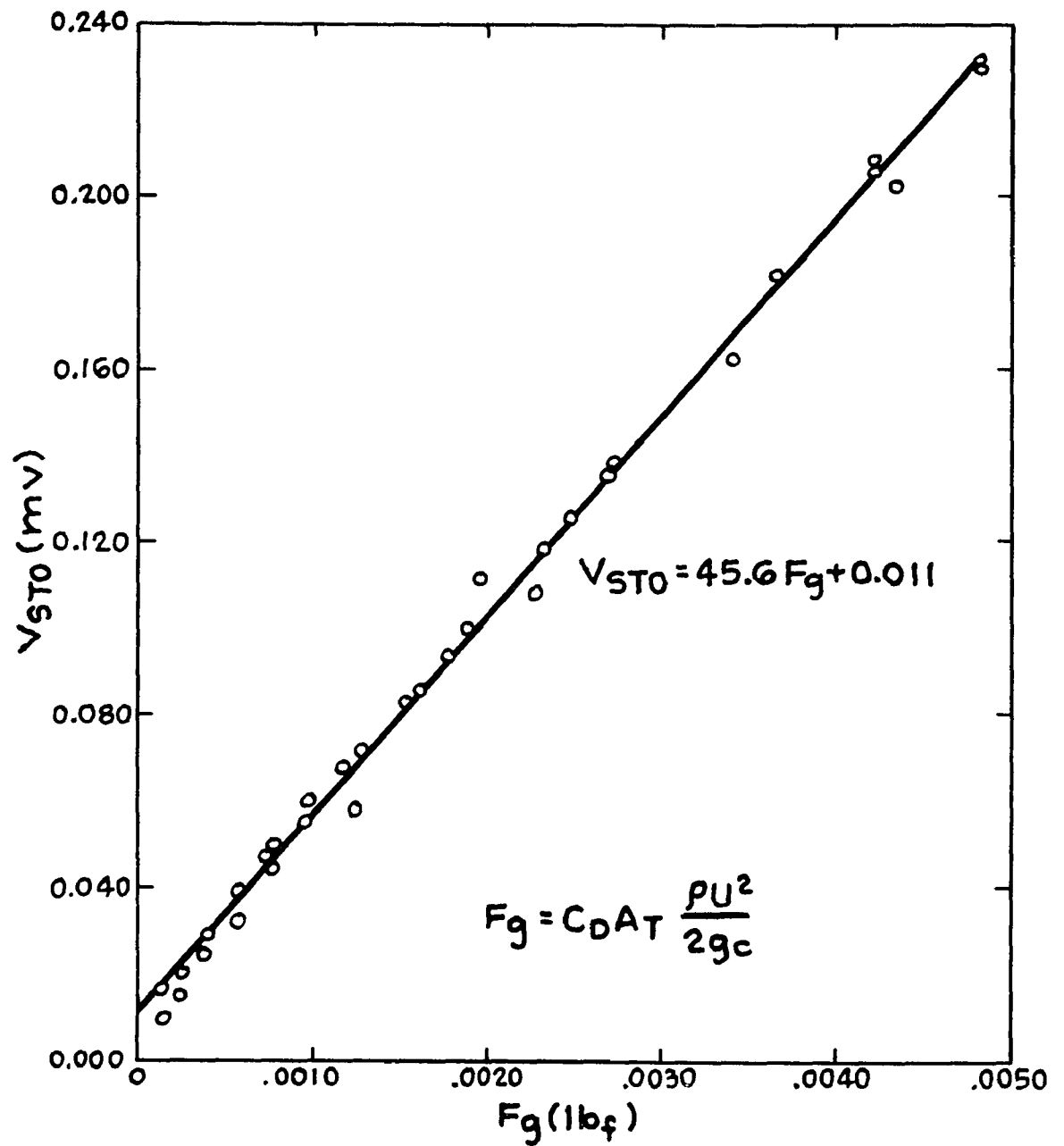


Figure 13
Recorder Output Voltage Versus Air Drag Force

time on the target area: $(2mV_p) \times (NV_p A_T)$. Assuming an ideal case of uniformly distributed, perfectly elastic particles, all traveling at the same velocity and striking the target normally only once, the force on the target would be precisely the expression:

$$F_p = \frac{1}{9c} 2mNV_p A_T V_p = \frac{1}{9c} 2\rho_{ds} V_p A_T V_p \quad (15)$$

or $F_p = \frac{1}{9c} 2 \frac{A_T}{A_p} W_p V_p$

Because the average particle velocity was not known, equation 15 was written as:

$$F_p = 2 \frac{A_T}{A_p} W_p k_1 U \frac{1}{9c} \quad (16)$$

where k_1 was the ratio of the average particle velocity to the average gas velocity. (The latter was calculated from the orifice reading.) Particles passing through the orifice were probably first decelerated in the stagnant region beneath the orifice plate and then accelerated upon reentrainment into the main gas stream. Consequently the particle velocity upon arrival at the target was not necessarily the same as the gas velocity. Therefore, k_1 was expected to vary with the ability of different particle sizes to accelerate to the gas velocity in the distance between the orifice and the target.

Recognizing some deviations from ideality, a further modification was made to equation 16:

$$V_{ST} - V_{STO} = c F_p = c 2 \frac{A_T}{A_p} W_p k_1 U k_2 \frac{1}{9c} \quad (17)$$

where the factor k_2 accounted for the deviations from the assumptions of the ideal case and, therefore, included the effects of several variables. In most cases the values of these variables approached those of the ideal case indicating a value of k_2 close to unity. Like the gain factor, disk drag coefficient, and proportionality factor c , the factors k_1 and k_2 had to be constant for the loading ratio calibrations to be valid, although their individual values were not required.

Because of the high coefficient of restitution of the glass beads, the collisions were nearly elastic. The wide dispersion of the particles in a dilute suspension prevented any tendency toward random velocity direction caused by particle-particle interactions. Therefore, the particles probably did strike the target normally. Multiple impacts by individual particles would have had a negligible effect upon the force of the particles striking the target as the velocity of the second impact would be of the order of the small particle settling velocity.

The collision efficiency of the disk was probably near unity. The order of the Stokes number for the glass beads indicated that their collision efficiency near a sphere was almost unity (Boothroyd 1971:34). Because the changes in the streamlines of the confined flow around the disk are more severe than around a sphere in an infinite flow, the beads could not have been able to follow the gas streamlines and avoid striking the target.

Flow pulsations, if averaged improperly by the strain gauge recording system, could have caused k_2 to vary between the limits of 1.0 (uniform flow) and 1.414 (rapid sinusoidal variations about the average flow rate). During open loop

calibrations, the rotary airlock caused some pulsations in the flow; however, they were too slow to be averaged by the filter and could not affect k_2 .

The one variable that probably affected the value of k_2 was the dispersed solids density. k_2 could vary between zero and A_p/A_T for the extreme cases of no particles being in the cylindrical space above the target or of all particles being in the cylindrical space above the target. Because of the nearness of the orifice, the density at the target was probably higher than the density around the target. The increase was estimated to be about 5 percent.

Equation 17 indicated that a plot of $V_{ST} - V_{STO}$ versus $A_T/A_p \cdot W_p \frac{U}{p g c}$ was linear. These plots are given in Figure 14 for open loop data obtained with the #279, #380, and #660 glass beads. W_p was determined in open loop operation by feeding and collecting a known mass of solids in a known time interval. U was calculated from the orifice manometer readings. The scatter in the data was caused by the difficulty of maintaining uniform feed rates and pump speeds during open loop operation. The figure indicated that $V_{ST} - V_{STO}$ was linear, within approximate 10 percent limits, with $A_T/A_p \cdot W_p \frac{U}{p g c}$ for given particle sizes. The relationship varied with particle size as expected from the nature of the factor k_1 . However, the combined values of k_1 and k_2 were constant for a given particle size.

Because the proportionality factor c between force and strain gauge reading should not depend on the nature of the medium producing the force, Figure 14 and an estimate for c from Figure 13 could be used to estimate the value of k_1 for the three particle sizes. Assuming that k_2 was independent of particle diameter and approximately equal to 1.05, the

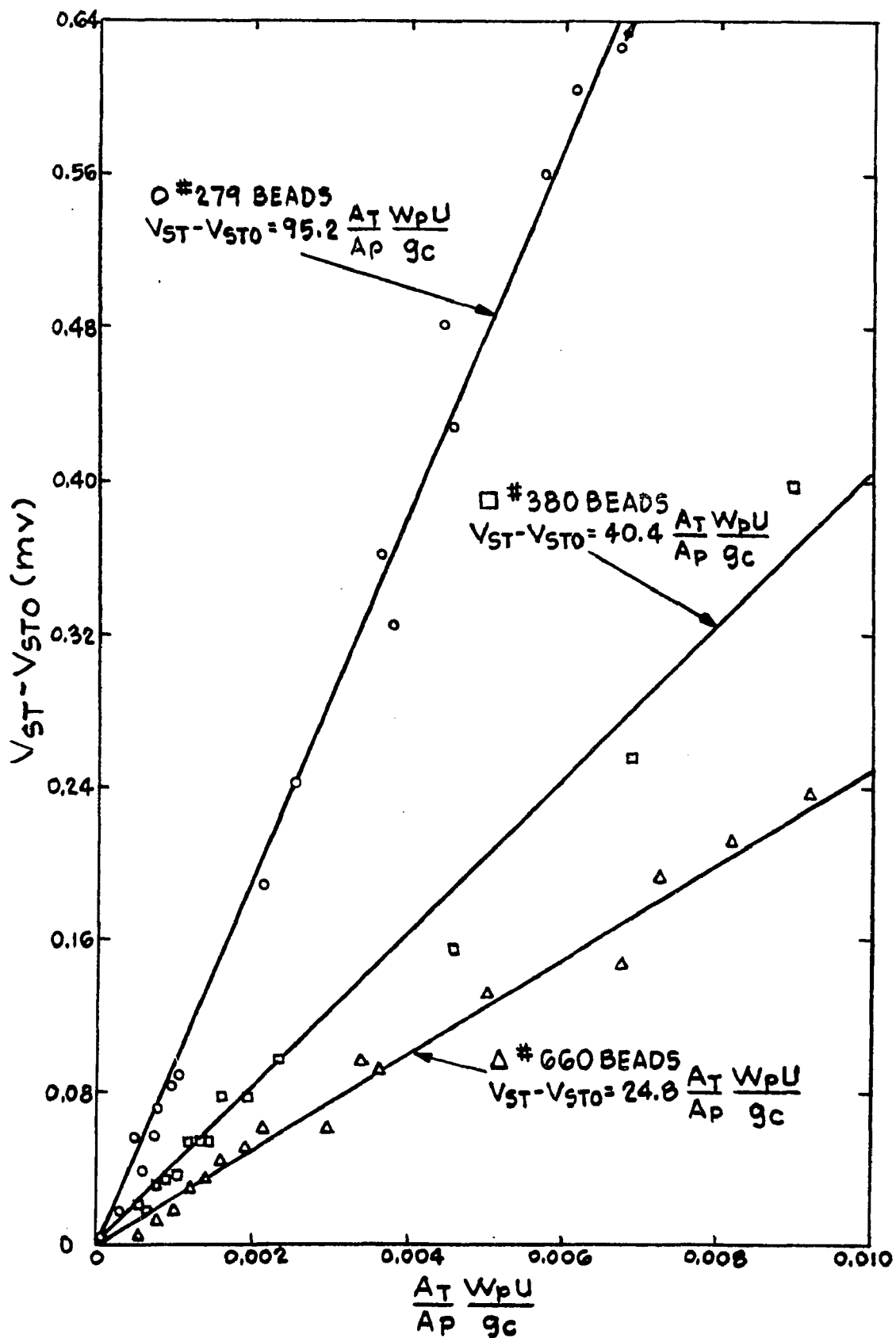


Figure 14

Recorder Output Voltage Versus Solids Drag

values of k_1 were 1.0, 0.4, and 0.26 for the #279, #380, and #660 glass beads respectively. These values were consistent with the relative values of particle relaxation time, t_R (0.00368, 0.01022, 0.0231 seconds) and the gas residence time (approximately 0.01 seconds) in the distance between the orifice and the target. Apparently the #279 beads had had sufficient time to accelerate to the gas velocity before striking the target. This was not true for the larger #380 and #660 beads.

The force equations, as such, were not required to develop the loading ratio calibrations. These were developed using the simpler empirical equations:

$$V_{STO} = 0.00106 \rho U^2 + 0.011 \quad (18)$$

for the air and

$$V_{ST} - V_{STO} = 0.977 W_p U \quad (19)$$

$$V_{ST} - V_{STO} = 0.415 W_p U \quad (20)$$

$$V_{ST} - V_{STO} = 0.244 W_p U \quad (21)$$

for the #279, #380, and #660 glass beads respectively.

The proven constancy of the various multiplicative factors in the force equations permitted lumping of all the constants into single empirical constants. Therefore, equations 18 through 21 were combined to give the following loading ratio formulae:

$$\eta = \frac{W_p}{\rho U A_p} = \frac{248}{\rho U^2} [V_{ST} - 0.011] - 0.264 \quad (22)$$

$$\eta = \frac{W_p}{\rho U A_p} = \frac{583}{\rho U^2} [V_{ST} - 0.011] - 0.620 \quad (23)$$

$$\eta = \frac{W_p}{\rho U A_p} = \frac{952}{\rho U^2} [V_{ST} - 0.011] - 1.011 \quad (24)$$

for the #279, #380, and #660 glass beads respectively.

Open loop calibrations were not performed for the #980 and #981 glass beads and the H-322 aluminum powder. The required inventory of #980 glass beads was too expensive, at 100 dollars per pound, to make open loop calibrations practical. However, the reasoning applied to the force equations indicated that the value of k_1 was already near if not at a maximum for the #279 glass beads. Therefore, the #980 glass beads, with a smaller (0.001776 seconds) relaxation time, were probably not moving any faster than the #279 glass beads (at the same carrying velocity) upon target impact. Therefore, the same force equation and loading ratio calibration were applied to both bead sizes.

The #981 glass beads were almost exactly the same size as the #279 glass beads and separate calibration was obviously not required. The H-322 aluminum powder represented a serious health hazard and open loop calibration was not desirable. An interpolated equation for $V_{ST} - V_{STO}$ was obtained for the powder by plotting the constants in equations 19 through 21 versus weight mean diameter on a log-log plot and locating the constant corresponding to the powder weight mean diameter. The interpolation gave:

$$V_{ST} - V_{STO} = 0.551 \frac{W_p U}{\rho} \quad (25)$$

and

$$\eta = \frac{W_p}{\rho U A_p} = \frac{440}{\rho U^2} [V_{ST} - 0.011] - 0.468 \quad (26)$$

for the H-322 aluminum powder. The formulae for loading ratio given by equations 22, 23, 24, and 26 were used for all suspension tests.

Clean Air Friction Factors

The pressure drop measurements were used to calculate friction factors from the equation:

$$f_g = \frac{2 \Delta P g_c D}{\rho U^2 L} \quad (27)$$

No corrections to the measured pressure drop were required during clean air flow measurements.

The static head of air between the two vertical manometer taps had no effect upon the micromanometer readings. The differential micromanometer was physically located beneath the lower (high pressure) tap and was acted on both sides by the same vertical head of air. The upper (low pressure) tap provided this head in the form of the vertical column of air in the micromanometer lead. The lower tap provided the identical head by adding the vertical column of air in the lead to the vertical column of air in the test section. Hence, the same static head was found on both sides of the micromanometer reservoir and was balanced automatically.

The density was calculated using total pressures measured at the first tap in each test section. Because the pressure drops across the test sections were negligible compared to the total pressure, corrections accounting for the

average total pressure in the test sections were not required. Temperatures for both test sections were assumed to be the same as those measured by the mercury in glass thermometer located above the anemometer probe. The average velocity in the horizontal test section was taken as the average velocity calculated from the orifice manometer reading. The calculation of the average velocity in the vertical test section accounted for the difference in density between the gas at the orifice and at the vertical test section.

The results of the clean air measurements are shown in Figure 15 where the Blasius friction factor is plotted against the gas Reynolds number. There was no discernible difference between the results obtained in the vertical test section and those obtained in the horizontal test section. The Blasius equation:

$$f_g = \frac{0.316}{Re_g^{0.250}} \quad (28)$$

which is considered accurate (Streeter 1962:217) to $\pm 5\%$ could have been used to correlate the data to within 3%. However, the empirical equation:

$$f_g = \frac{0.524}{Re_g^{0.302}} \quad (29)$$

was used to correlate the data to $\pm 1\%$. In the working range of Reynolds numbers used in the study, the empirical equation predicts slightly higher friction factors than does the Blasius equation. However, this was perfectly consistent with the standard Moody diagram (Streeter 1962:Fig. 5.34) and the small but finite roughness, 0.000 005 feet,

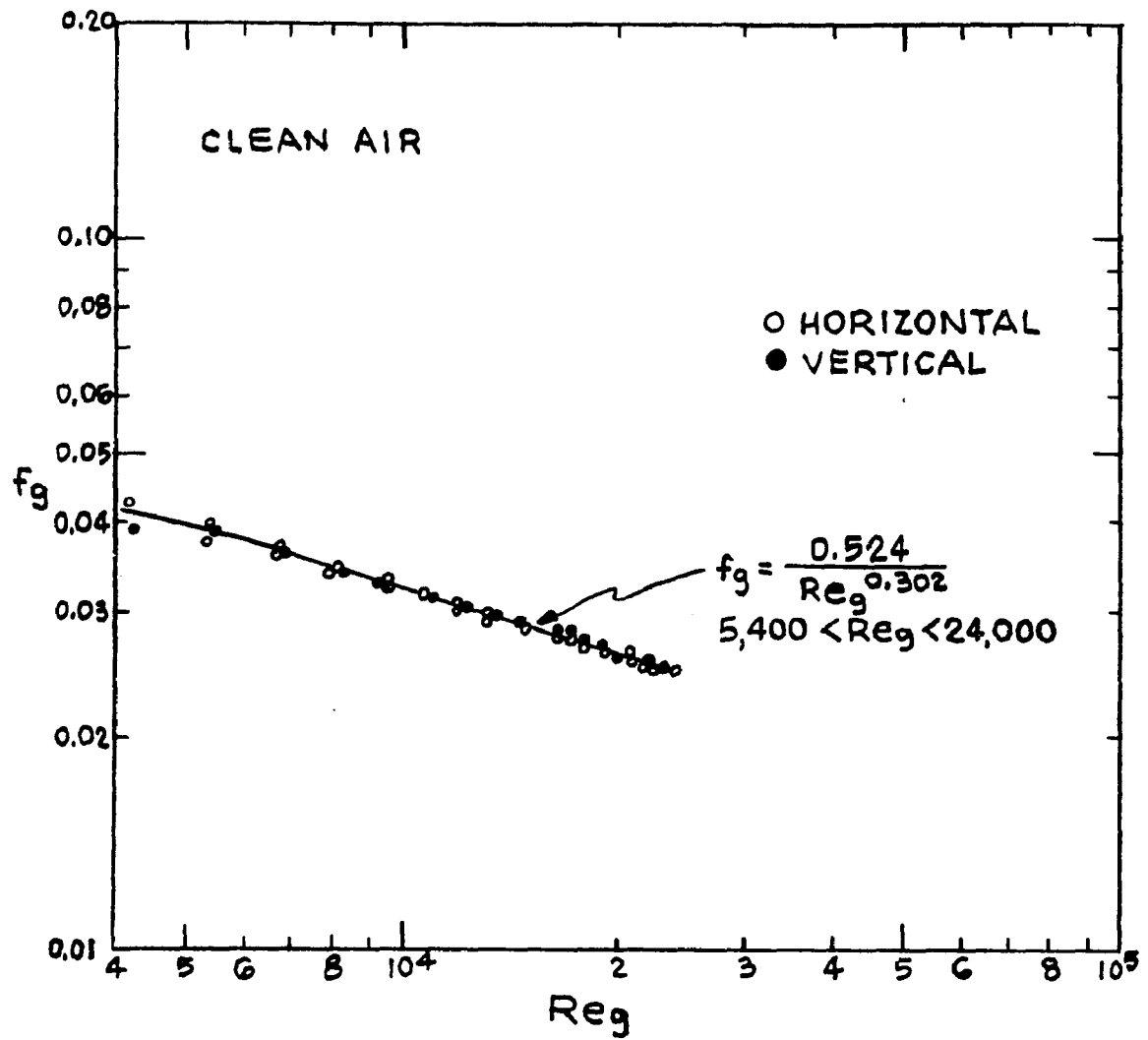


Figure 15

Blasius Friction Factor Versus Gas Reynolds Number

associated with commercially drawn tubing. Friction factors for clean air were checked both before and after solids circulation and found to be correlated by the same empirical equation.

ANEMOMETER PROBE CALIBRATIONS

Three probe types, thermistor, hot-wire and fiber-film, were used for meaningful measurements and were, therefore, subjected to extensive calibration. Despite their different construction, the relationship between velocity and D.C. voltage for all three probes was quite similar. Only the thermistor probe had significant sensitivity to fluid temperature changes.

The calibration curves, at a fixed temperature for the thermistor, followed a generalized form of an equation derived by King in 1914. King's law (Hinze 1959:79), as it is commonly called, is given below:

$$I^2 R_w = e k_g l (T_w - T_g) \left(1 + \sqrt{2\pi \frac{\rho c_{pg} d U}{k_g}} \right) \quad (30)$$

valid for $\rho c_{pg} d U / k_g > 0.08$. King's law was theoretically derived for the case of a uniformly heated cylinder in a two-dimensional, incompressible, potential, inviscid flow. It describes the heat transfer from an ideal hot-wire to an ideal fluid. No anemometer probe conforms to the idealized case and all probe suppliers recommend the following empirical equation to describe the probe operation in constant property fluids.

$$v^2 - v_o^2 = B U^n \quad (31)$$

The factor B , exponent n , and no-flow voltage V_o must be determined experimentally. B and V_o are strong functions of the overheat ratio but are essentially constant in air if the overheat ratio (or probe resistance) is constant.

Thermistor

Figure 16 shows a logarithmic plot of $V^2 - V_o^2$ versus U for the thermistor probe. The calibration was strongly dependent upon temperature as seen in the figure. The calibration for this type of probe was quite lengthy as a plot of voltage versus velocity, with temperature as a parameter, was required. Because of the extreme temperature sensitivity, the temperature at the probe location during calibrations had to be corrected for the approximately isentropic temperature decrease between the wind tunnel reservoir and measuring station. Although the temperature decrease was at most 0.9°F , the effect on the calibration curve was large.

Regardless of fluid temperature, the exponent n remained constant at 0.477 which was surprisingly close to the value of 0.500 for the ideal case of King's law even though the thermistor shape is more like a sphere than a cylinder. The curves deviated from the empirical equation at low values of velocity as suggested by the limitations on King's law. At the higher velocities, the calibration curves were well described by the formulae indicated on Figure 16. For convenience, interpolated formulae were determined for fluid temperatures of exactly $75, 76, 77, 78, 79$, and 80°F and listed on the figure.

For convenience during measurements, Figure 17 was prepared. The thermistor was the only probe that was operated using the auxiliary unit zero-adjust and gain

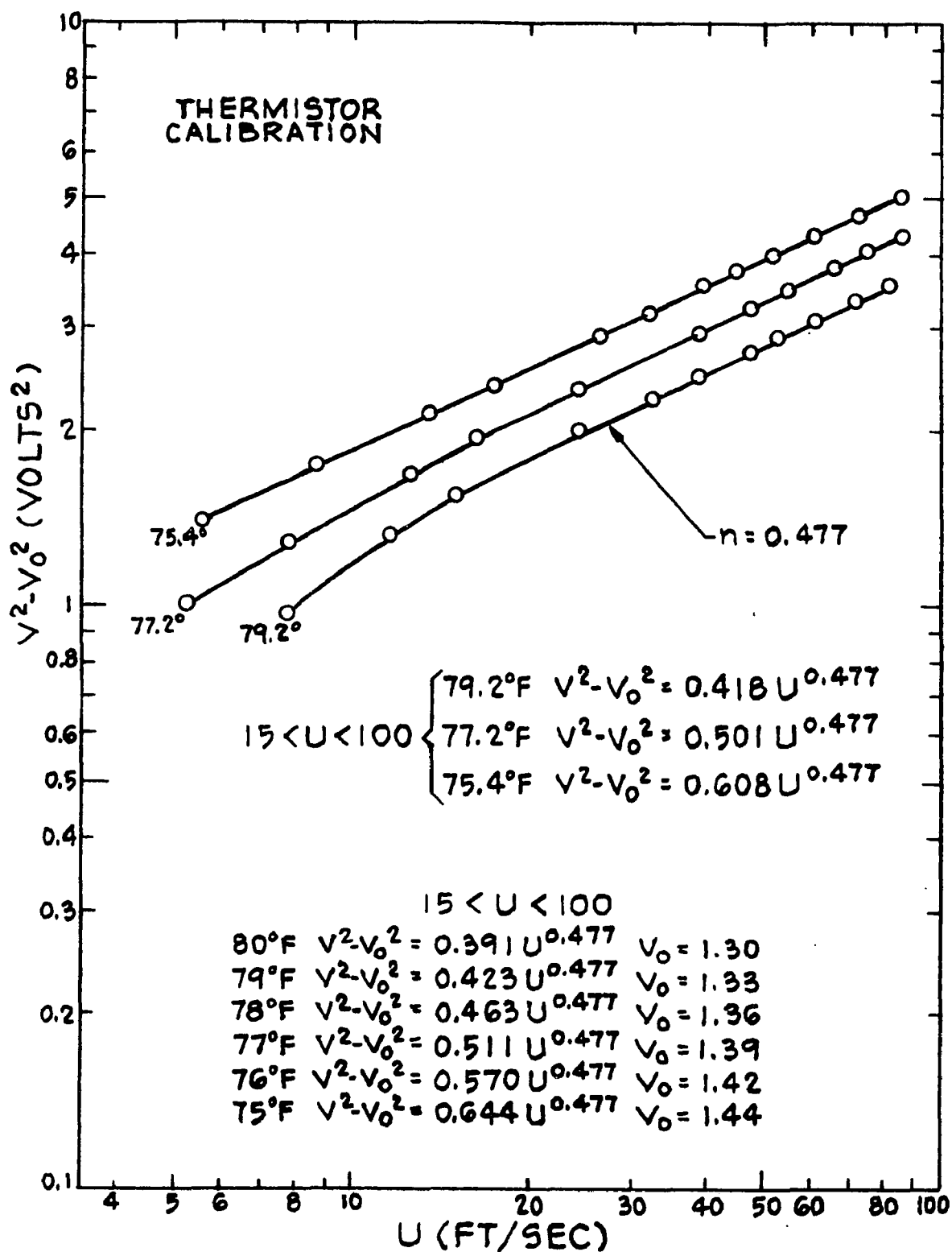


Figure 16

Thermistor Calibration--Squared Voltage Versus Velocity

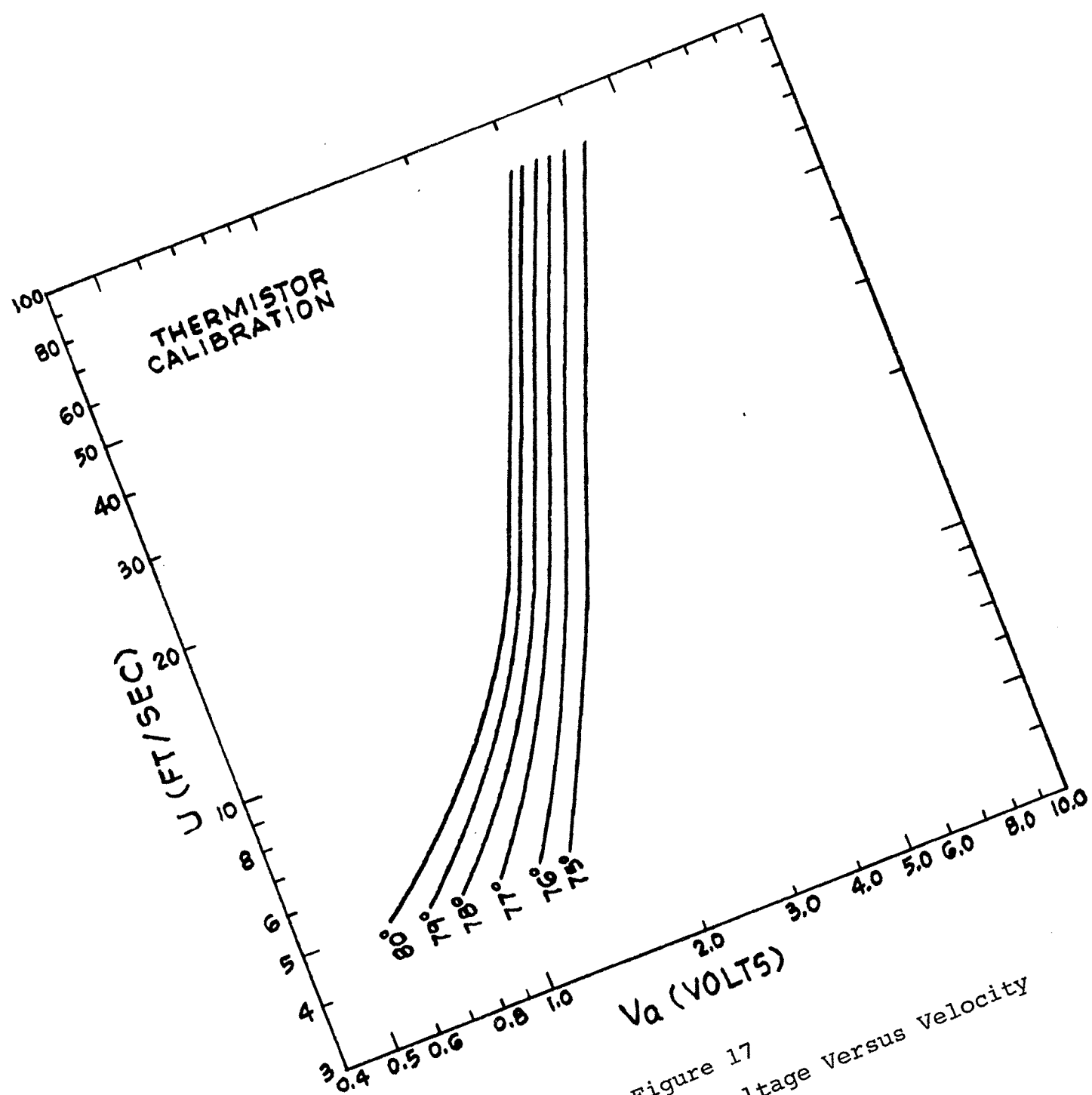


Figure 17
Thermistor Calibration--Voltage Versus Velocity

controls. The voltage V_a on Figure 17 represented the reading of the D.C. voltmeter after the bridge output voltage V had been translated by the zero adjust control and multiplied by the gain control of the auxiliary unit. Figure 17 is then a plot of U versus $m(V - V_o')$. The gain m was set permanently at 3.30 and V_o' was set permanently at 1.43 volts. V_o' corresponded to the no-flow voltage at 75.7°F .

Hot-Wire

The calibration of the hot-wire probe was considerably simpler than that of the thermistor probe since a plot with temperature as a parameter was not required. Figure 18 shows the relationship between $V^2 - V_o^2$ and U . With the exception of the lowest velocity point, the exponent of the curve corresponded exactly to the value of 0.500 predicted by King's law. Figure 19 is a plot of U versus V and reveals the excellent precision attainable with the wind tunnel.

Fiber-Film

Seven fiber-film probes were calibrated during the course of this investigation. The individual values of calibration constants varied from one probe to another but all seven exhibited the same behavior. Like the hot-wire probe, a plot with temperature as a parameter was not required. Also like the hot-wire probe, the fiber-film probe calibration was a strong function of overheat ratio.

Figure 20 shows the results of two calibrations performed at different temperatures but at the same overheat ratio. The figure confirmed that separate temperature dependent calibrations were not required provided the overheat ratio was maintained constant. Figure 21 shows the effect of the overheat ratio upon the voltage-velocity

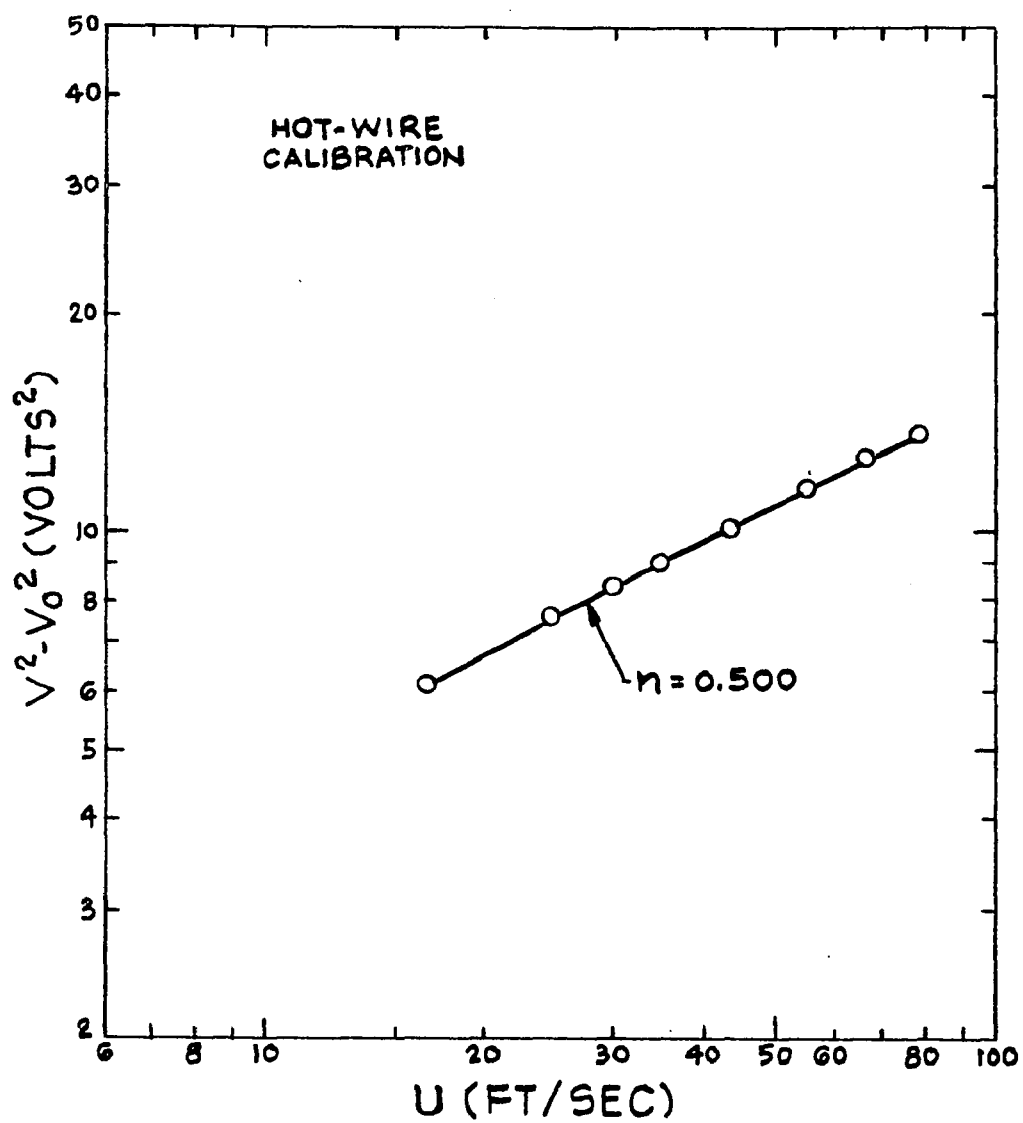


Figure 18

Hot-Wire Calibration--Squared Voltage Versus Velocity

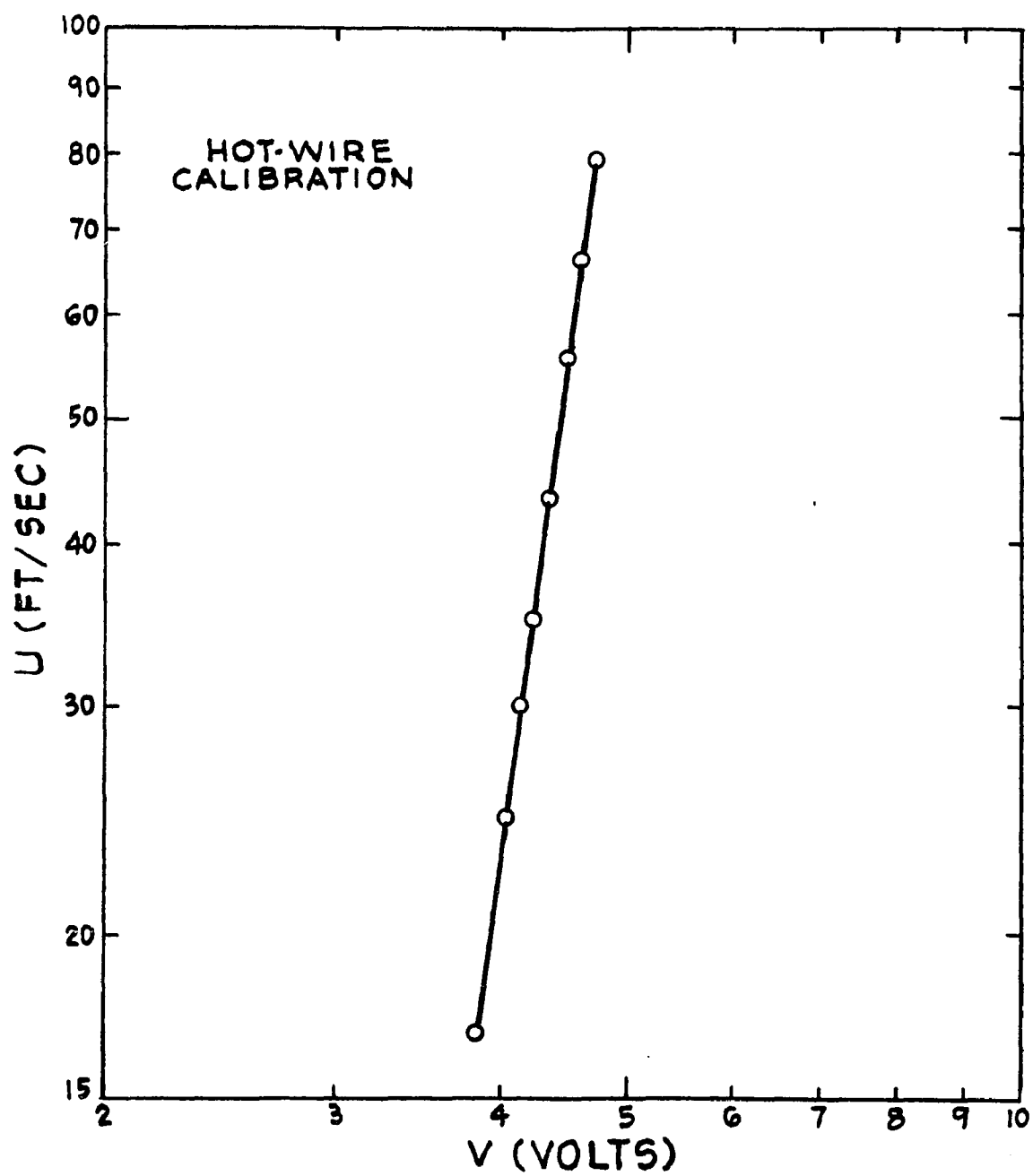


Figure 19

Hot-Wire Calibration--Voltage Versus Velocity

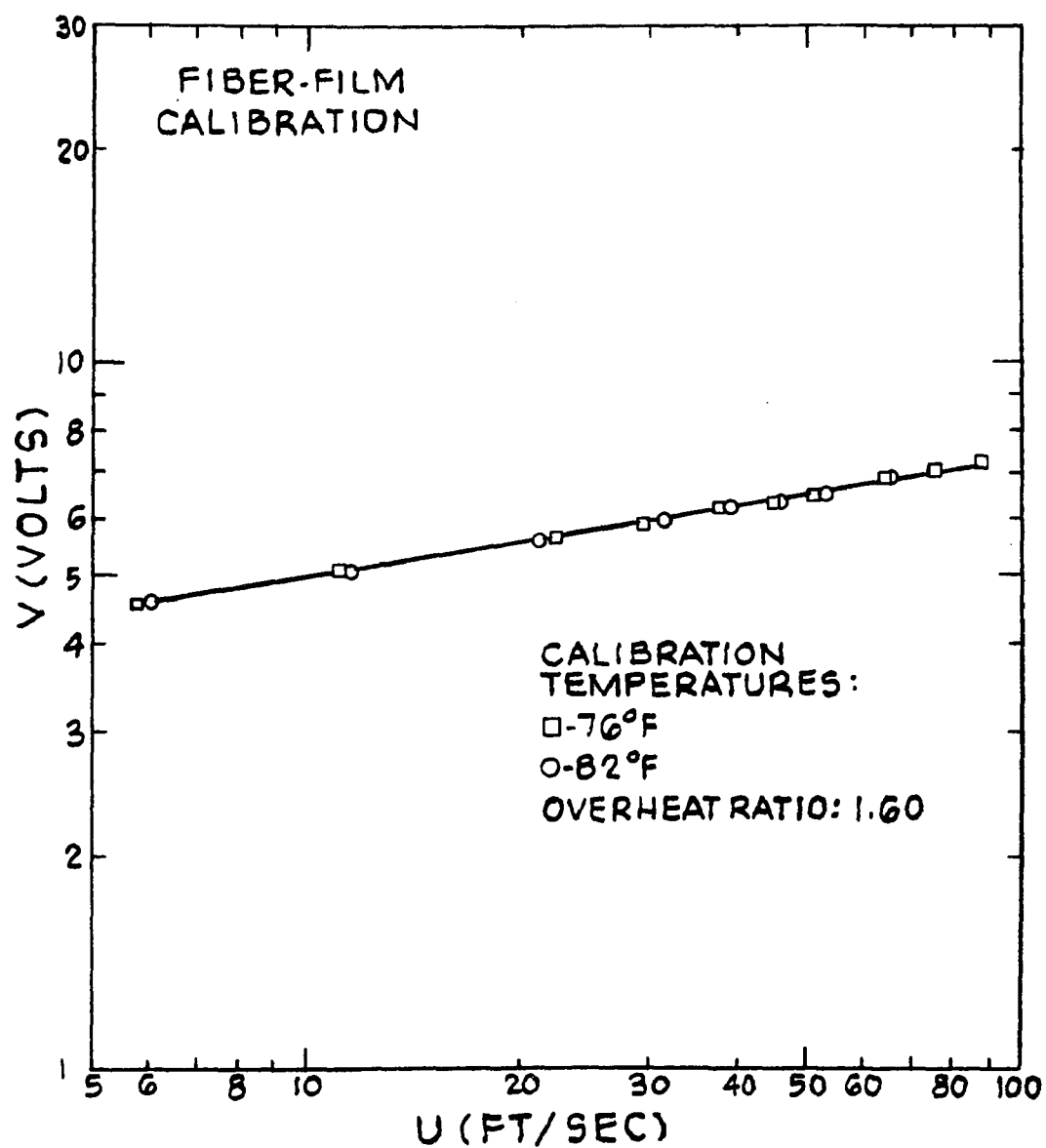


Figure 20
Fiber-Film Calibration--
Voltage Versus Velocity--Constant Overheat

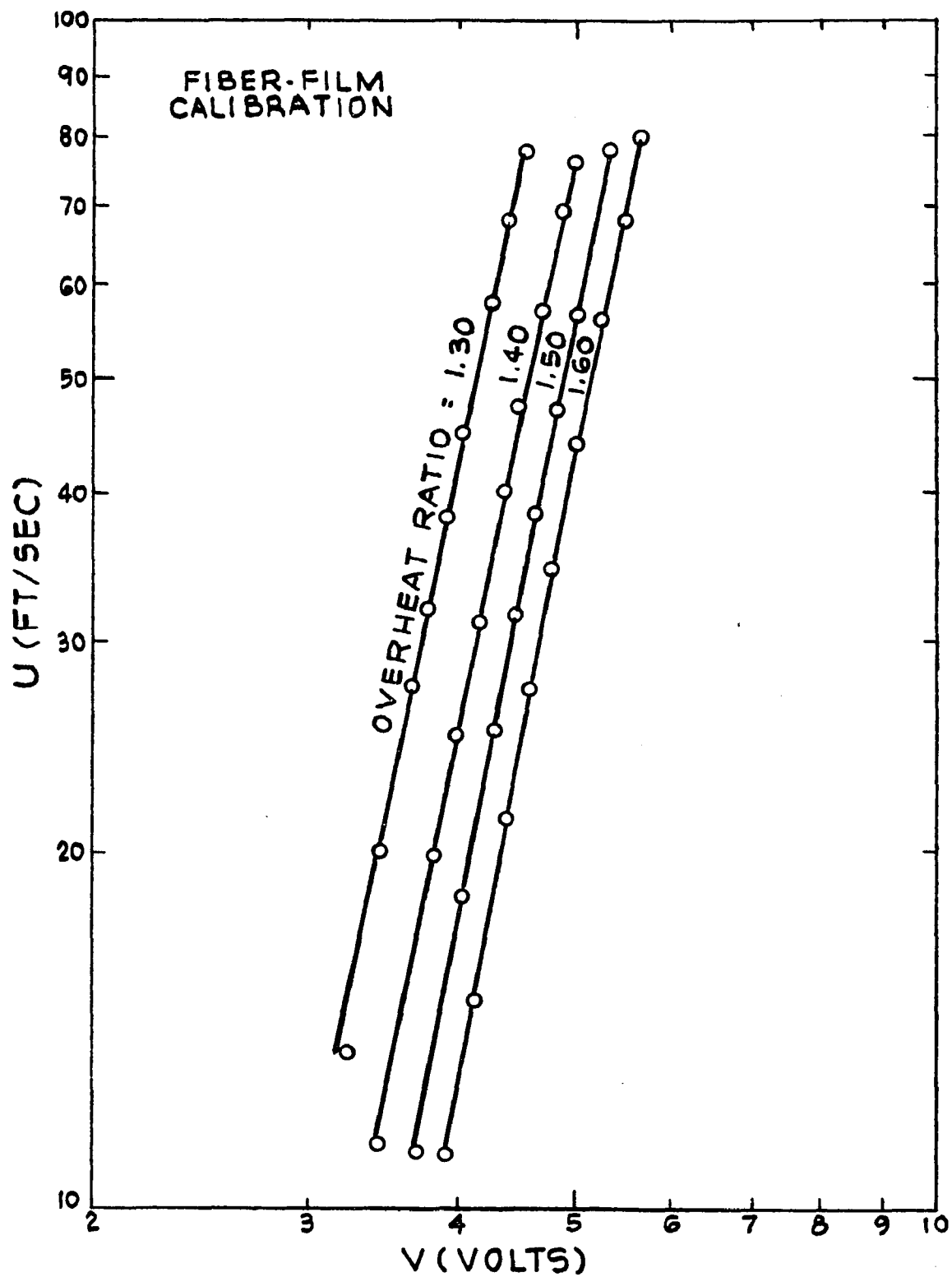


Figure 21

Fiber-Film Calibration--
Voltage Versus Velocity--Variable Overheat

relationship. Variations in overheat ratio caused parallel translation of calibration curves. Figure 22 shows that the parallel translation did not affect the value of the exponent n for a particular probe. The value of n varied among different fiber-film probes but remained around 0.500. Figure 23 shows that the effect of particle contamination also consisted of parallel translation of the calibration curves.

CLEAN AIR VELOCITY PROFILES

Clean air velocity profiles were taken with all three probe types. The profiles taken with the hot-wire and the fiber-film probes showed the least scatter as temperature sensitivity was negligible.

Figure 24 shows the fiber-film profiles plotted as U/U_c versus r/R . Even the small amount of scatter here was large enough to mask the slight variation of the velocity profile with Reynolds number that was discerned by Nikuradse (Schlichting 1968:562). A single power law profile:

$$\frac{U}{U_c} = \left(\frac{y}{R}\right)^{1/6.62} \quad (32)$$

was chosen to fit the data. Schlichting (Schlichting 1968:563) reports that the exponent, which increases with Reynolds number, has the value $1/6.60$ at a Reynolds number of 23,000. The measured profiles were, therefore, only slightly flatter than expected; the additional flatness perhaps the result of the disturbances produced by the anemometer inlet port and the probe support.

The power law, as reported by Schlichting, begins to deviate from experimental data at y/R of about 0.8. This is in agreement with the results reported in Figure 24. The

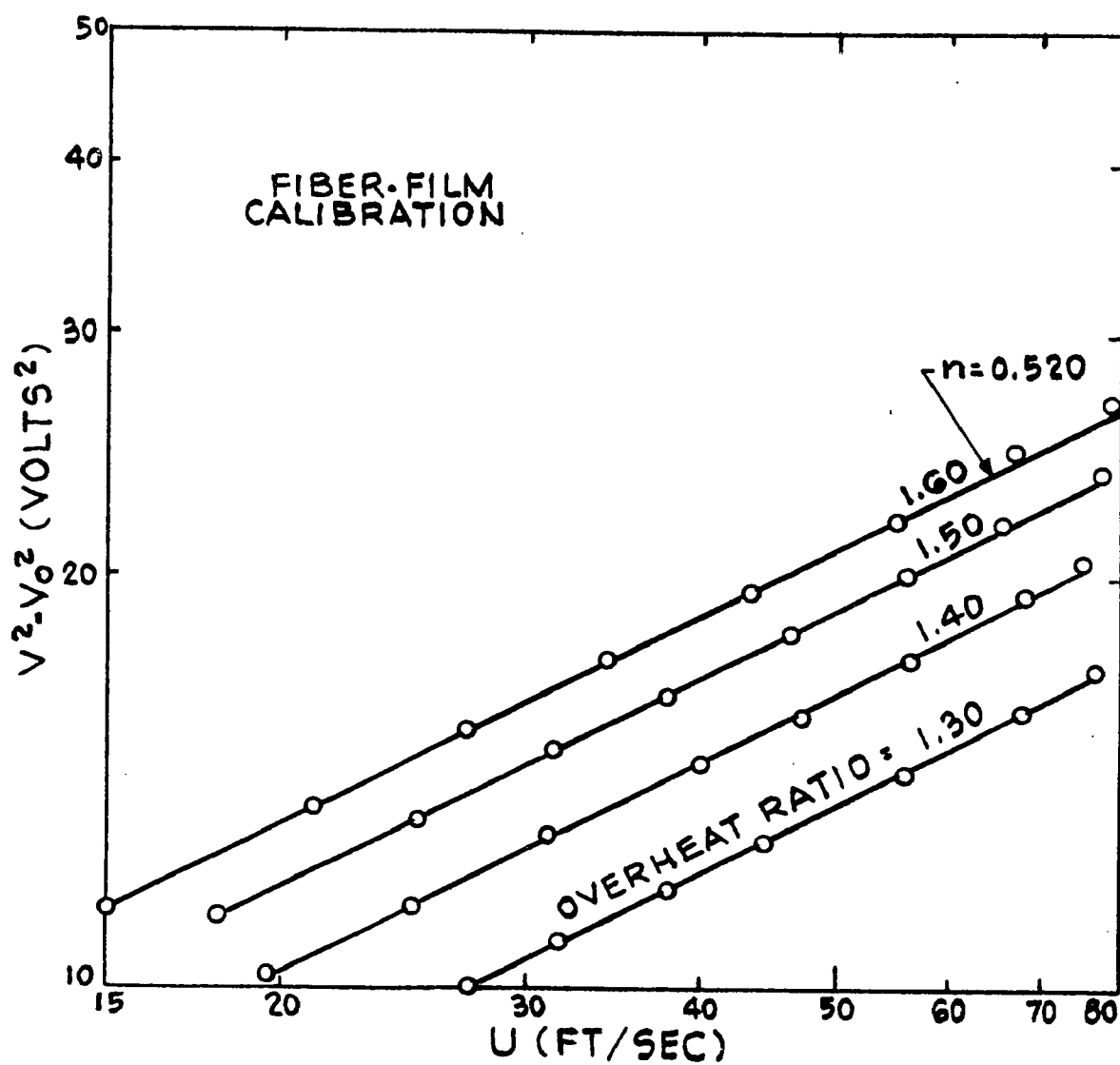


Figure 22

Fiber-Film Calibration--

Squared Voltage Versus Velocity--Variable Overheat

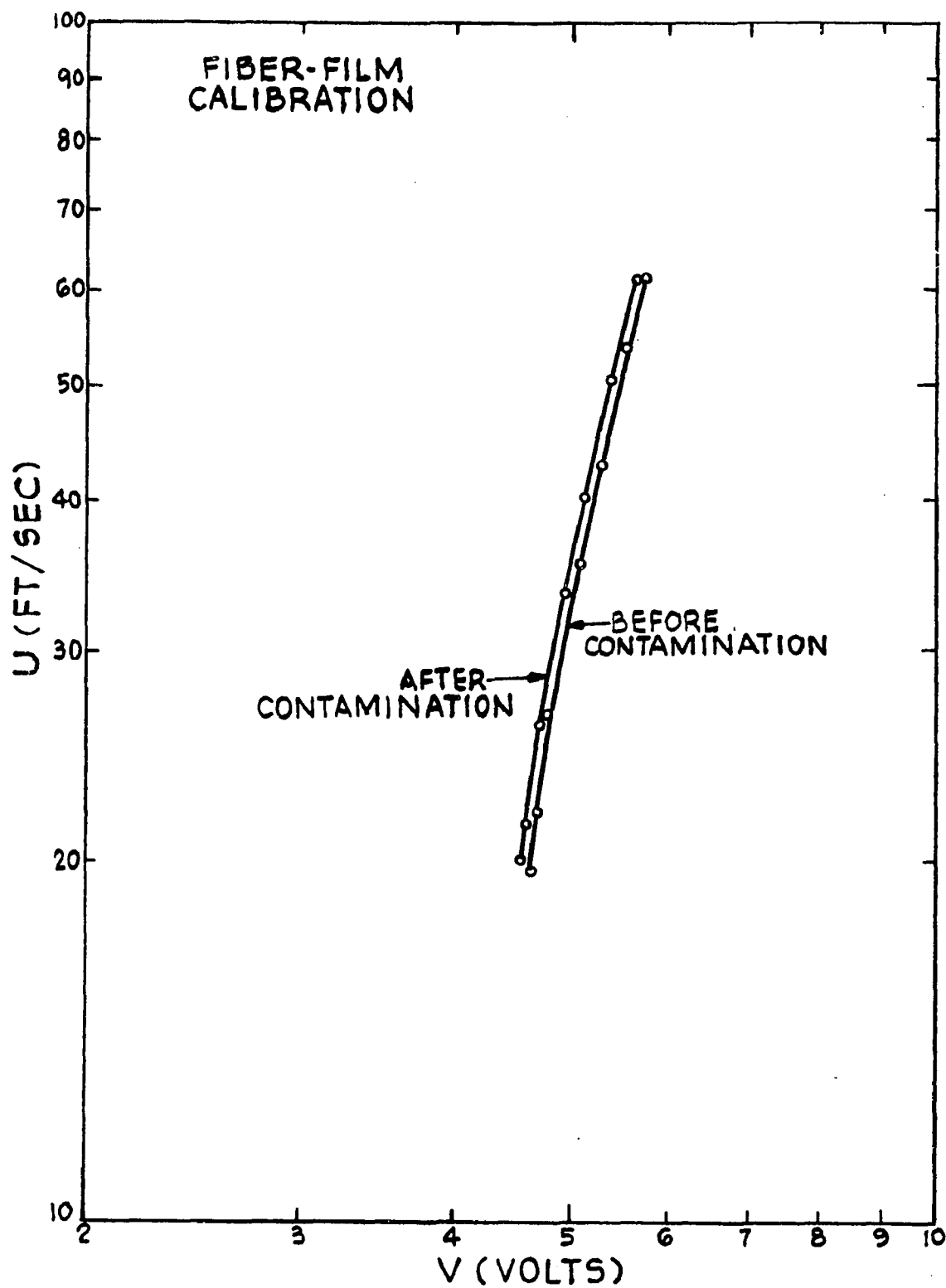


Figure 23.

Fiber-Film Calibration--Effect of Contamination

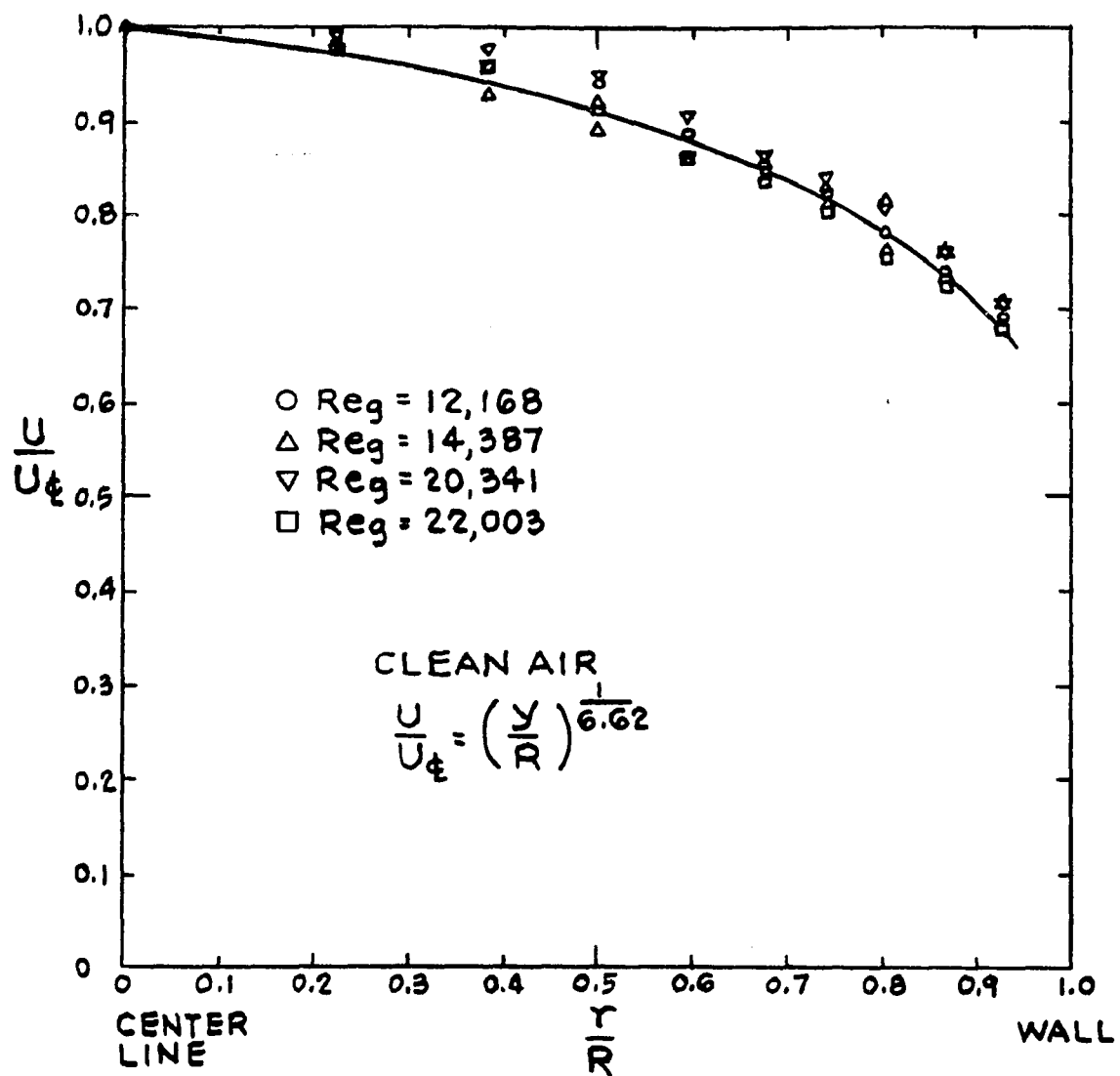


Figure 24
Clean Air Velocity Profiles

measured profiles confirmed the fully developed condition of the flow at the vertical test section and the assumed ratio of the average velocity to the centerline velocity for clean gas flow.

Figure 25 shows the velocity profiles replotted in terms of the friction velocity and the universal velocity profile. Although there is considerable disagreement about the appropriate constants in the logarithmic law of the wall (Monin and Yaglom 1971:276-278), the law as reported by Schlichting (Schlichting 1968:567) has been drawn on the figure to permit comparison with the data. The agreement is quite good, except at large values of yU_*/ν where U/U_* exceeds the value predicted by the law. This type of divergence is not unusual having also been reported in separate studies by Klebanoff, Deissler, and Van Driest (Monin and Yaglom 1971:273). The figure also shows that the traverse carried only into the outside edge of the buffer layer and not at all into the viscous sublayer.

CLEAN AIR TURBULENCE PROFILES

The measurement of clean air turbulence profiles revealed that the numerical results, although not necessarily the trends, were strongly dependent on the type of probe and the overheat ratio. The results indicated that comparisons of the numerical results, again excepting trends, obtained by different investigators are not valid unless identical measurement techniques are used.

Turbulence Intensity Profiles

In this study, turbulence intensity profiles were measured with three different types of probes in clean gas

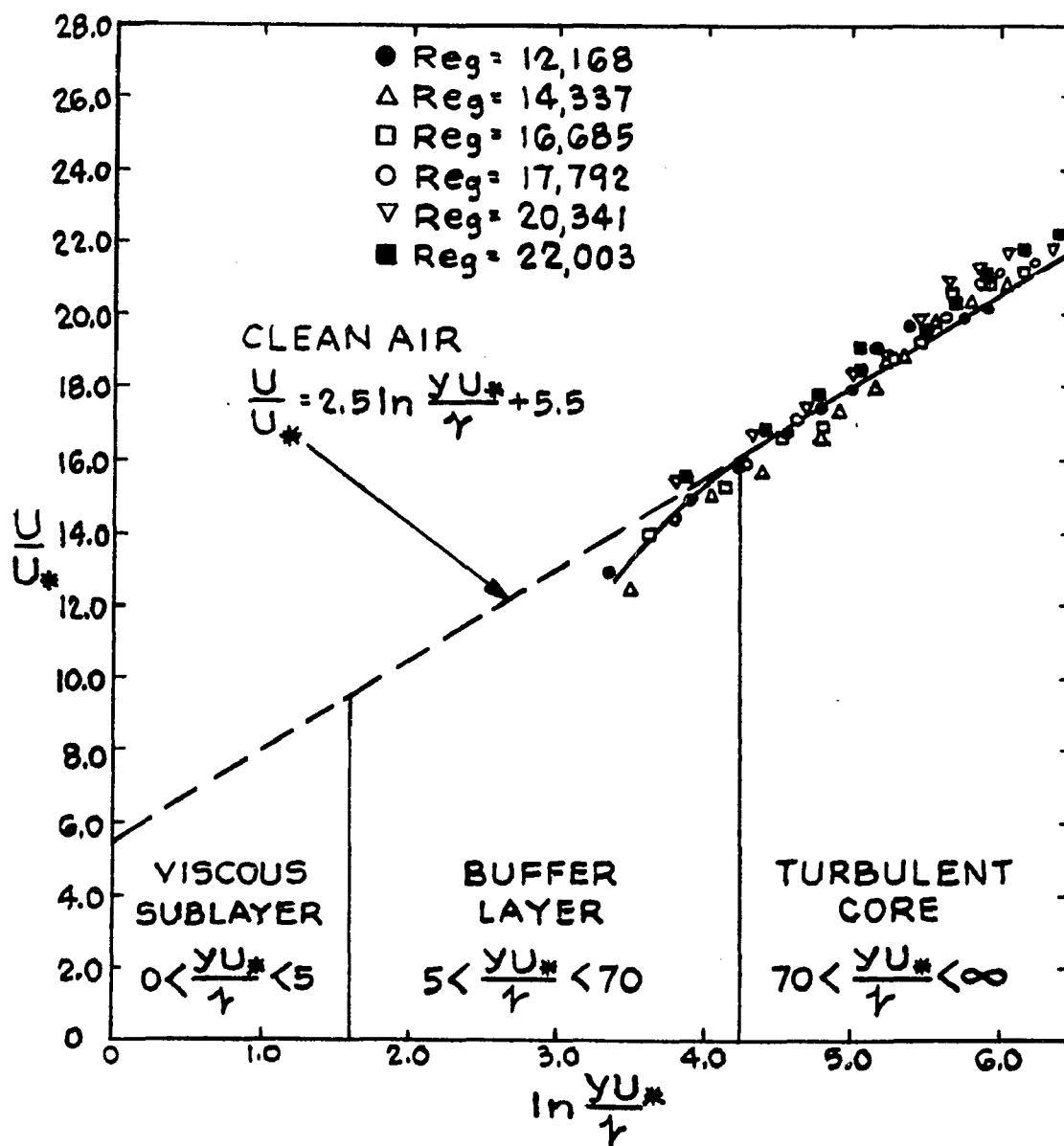


Figure 25
Clean Air Velocity Profiles and
the Universal Velocity Profile

flow. Under identical external conditions, the turbulence intensities recorded by the three distinct types of probes were notably different. Even using the same probe type, the hot-wire for example, different results were obtained if the overheat ratio was changed. Only if the probe type and overheat ratio were maintained constant did reproducible results occur.

In contrast, the trends reported by all three probe types was extremely uniform and reliable. The trends included the proper direction of increased turbulence intensity as probe location and gas Reynolds numbers were varied.

The probe calibrations were used to calculate relative turbulence intensity directly from the anemometer voltages. For relative turbulence intensities less than 0.20, the percent relative longitudinal turbulence intensity (within an accuracy of 5 percent of the measurement) is given by (Rasmusson 1966:10):

$$100 \frac{\sqrt{u_i^2}}{U} = \frac{200 V V_{rms}}{n(V^2 - V_o^2)} \quad (33)$$

Thermistor. For the thermistor, the gain factor and zero adjustment was incorporated into the equation in the form:

$$100 \frac{\sqrt{u_i^2}}{U} = \frac{200 V_{arms} [V_a + m V_o']}{n [V_a^2 + 2m V_a V_o' + m^2 (V_o'^2 - V_o^2)]} \quad (34)$$

Figure 26 shows the results of some preliminary turbulence measurements made with the thermistor probes during

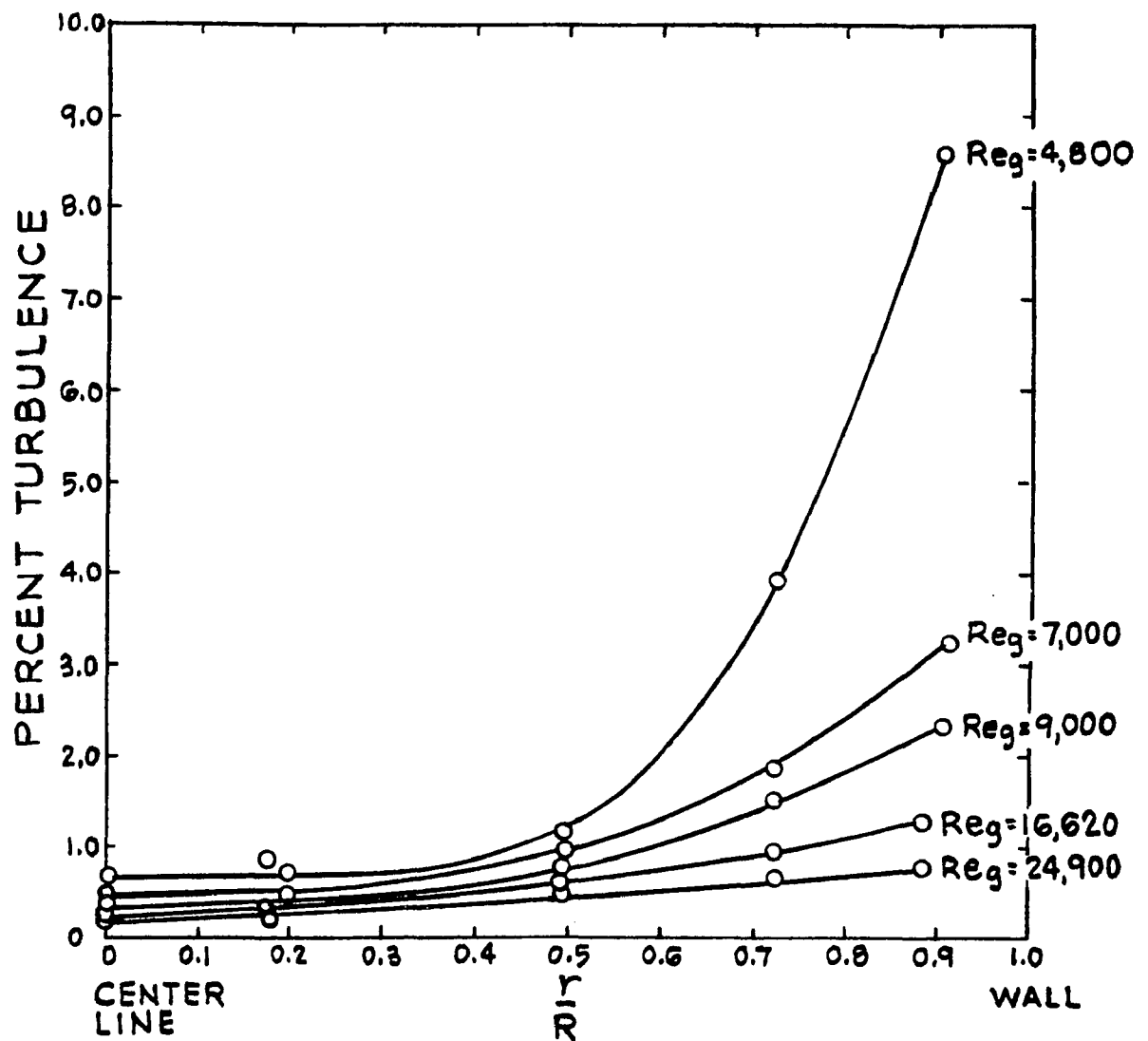


Figure 26
Clean Air Turbulence Intensities--
Thermistor Measurements

abbreviated 5 point traverses. (Ten point traverses were used for all subsequent traverses made with the hot-wire and fiber-film probes.)

The trends reported in Figure 26 are in agreement with the results of other investigators (Laufer 1954; Townes, Gow, Powe, and Weber 1971). The turbulence intensity relative to the local mean velocity increased from the tube centerline, slowly at first, and then rapidly as the wall was approached. The turbulence intensity relative to the local mean velocity at a fixed radial position decreased with increased values of Reynolds number. Because measurements were not taken deep in the buffer layer or in the viscous sublayer, the known sharp maximum at the edge of the sublayer and the known linear decrease to zero at the wall (Laufer 1954) were not observed.

The numerical values of the turbulence intensity were considerably lower than reported by other investigators working with even higher Reynolds numbers. For example, Laufer's data indicated (Hinze 1959:521) that the percent relative turbulence intensity at the centerline is 2.8 at a Reynolds number of 500,000. The thermistor recorded a percent relative turbulence intensity of only 0.23 at a Reynolds number of 24,900. The difference can be attributed to the difference in the probes used by Laufer, 1.27 and 2.54 μ diameter hot wires, and the probe used here, a 540 μ diameter thermistor. Because the sensitivity of a probe varies inversely with the probe diameter (Hinze 1959:85) the lack of sensitivity of the thermistor was not surprising. Consequently the thermistor was no longer used to measure turbulence intensities after these preliminary tests.

Hot-Wire. Figure 27 shows the results of turbulence measurements taken with the 5μ diameter hot-wire probe at two different Reynolds numbers. The marked improvement in the sensitivity of this probe over the thermistor is obvious. In order to determine the effect of overheat ratio on the probe sensitivity, the intensity at the tube centerline for a Reynolds number of 11,986 was measured at two different overheat ratios. The probe recorded a higher intensity when operated at the higher and recommended maximum overheat ratio of 1.80. This indicated that the greatest sensitivity could be achieved by operation at the maximum overheat ratio allowed for a particular probe type.

Fiber-Film. The fiber-film probe combined the characteristics of rugged construction and reasonable sensitivity to turbulence. This probe was chosen to measure gas turbulence in both clean air and suspension flow. The clean air results, plotted as percent turbulence intensity relative to local mean velocity versus radial distance, are shown in Figure 28 for six Reynolds numbers. Again the trends of turbulence intensity variation are clearly indicated.

The 70μ diameter probe did not possess quite the sensitivity of the hot wire but was far superior to the thermistor probe. The probe was operated at an overheat ratio of only 1.60; however, this was the maximum value permitted by the manufacturer. Operation at higher overheat ratios would have reduced probe life and increased the possibility of burnout. Despite the lower overheat ratio and the larger diameter, the probe registered between 60 and 70 percent of the turbulence registered by the hot wire.

Figure 29 shows the turbulence intensity normalized with the friction velocity and plotted against the

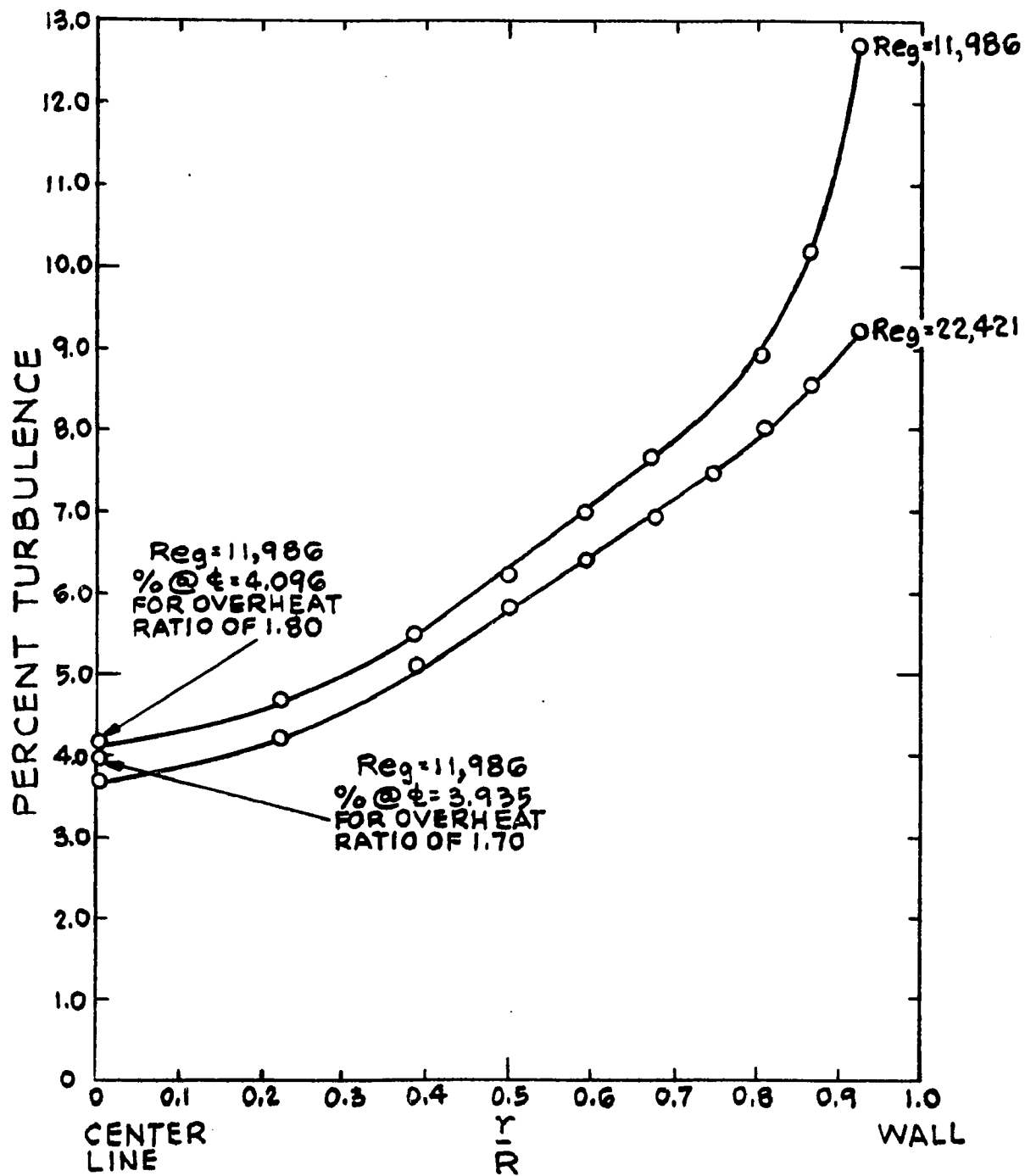


Figure 27
Clean Air Turbulence Intensities--
Hot-Wire Measurements

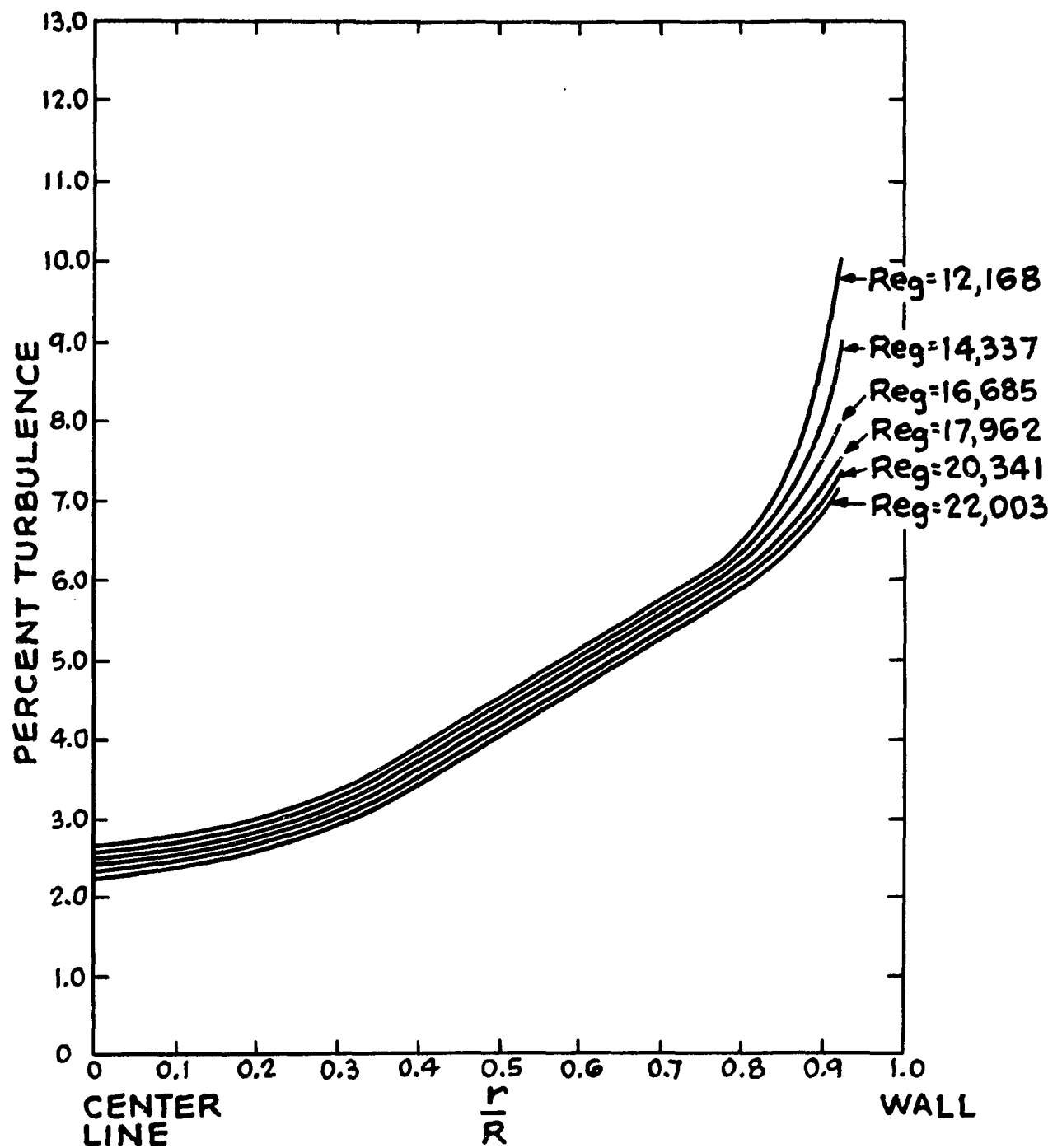


Figure 28
Clean Air Turbulence Intensities--
Fiber-Film Measurements

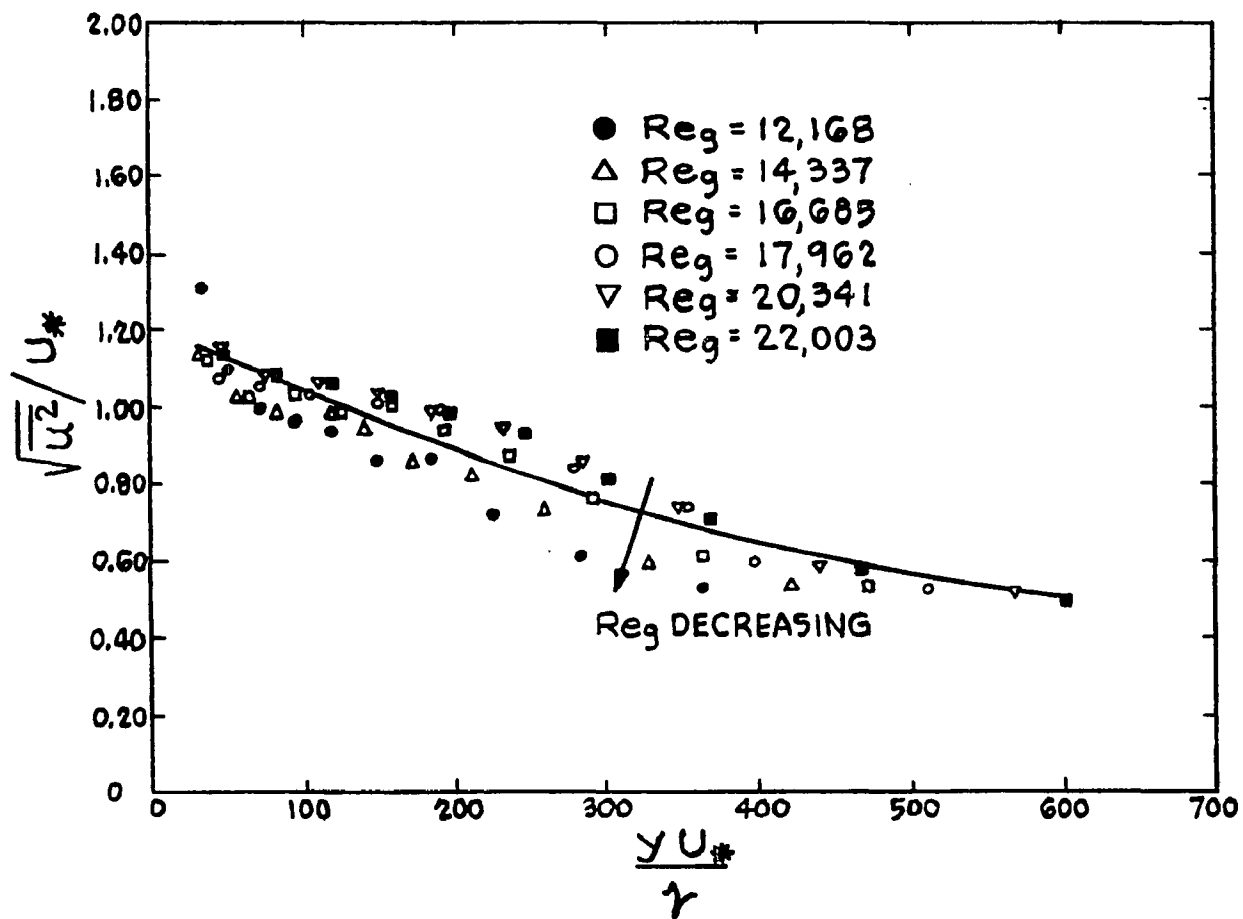


Figure 29 Clean Air Turbulence Intensities Normalized with Friction Velocity

non-dimensional distance from the wall. The agreement with the trends of Laufer's data was excellent. Normalization with the friction velocity brought the turbulence intensities for all six Reynolds numbers close to the same curve. At large values of y^* , the data were independent of Reynolds number. At intermediate values of y^* , turbulence intensities were larger for the higher Reynolds number flows. At low values of y^* , near the wall, the data merged indicating that turbulence intensities for the low Reynolds number flows increased more rapidly as the wall was approached. The same results were previously reported by Laufer (Laufer 1954) for flow at Reynolds numbers of 50,000 and 500,000. Laufer also reported that the turbulence intensities in the near wall region, $0 < y^* < 20$, were completely independent of Reynolds number. This was not observed in this study because the lowest value of y^* achieved was greater than 25.

The numerical values of the turbulence intensity were about 40 percent lower than reported by Laufer far from the wall. The difference increased slightly to about 50 percent near the wall. The variation in sensitivity across the tube was probably caused by the variation in the eddy energy distribution across the tube diameter. Near the wall, the smaller eddies contain more energy than they do far from the wall (Laufer 1954). Any loss of probe sensitivity because of large sensor dimensions will be most significant in regions where small eddies are an important part of the turbulent energy distribution. Therefore, the numerical results of this study were consistent with those of Laufer if the relative sizes of the probes are considered.

Turbulent Spectral Profiles

The spectrum analyzer produced a display of voltage

versus frequency for a particular root mean square voltage. The equivalent turbulence intensity was calculated for each frequency bandwidth and was divided by the bandwidth to determine the spectral power density. The conversion from the frequency to the wavenumber domain followed Taylor's hypothesis (Hinze 1959:40-42, 166). Although, as Hinze points out, the longitudinal spectrum represents only a one-dimensional cut of a three dimensional wave number spectrum, the results were cast in wavenumber form to permit comparison with Laufer's results.

Figure 30 shows the results of two spectrum analyses for the centerline and near wall positions at a Reynolds number of 20,341. The limitations of the probe and the inadequacies of the spectrum analysis were revealed clearly by this figure. There was a tendency toward an energy variation with $k^{-5/3}$ at wavenumbers and with k^{-7} at high wavenumbers as observed by Laufer. However, the steep k^{-7} region began at much lower wavenumbers than was previously reported. The spectrum analyses confirmed the comparative lack of sensitivity of the probe to small eddies. The spectrum analyzer itself prevented accurate measurements at very low wavenumbers because of the large size of the zero frequency marker and the high internal noise level. Therefore, an energy variation with k^{-1} was not observed even at traverse positions near the wall.

Laufer limited his data to wavenumbers not exceeding approximately 600 ft^{-1} . If this value is considered the limit of his reliable data, a linear scaling with probe diameter indicates that the limit of expected reliability of the fiber-film probe is only 22 ft^{-1} for spectrum analyses. The low wavenumber limit indicated that the loss of probe

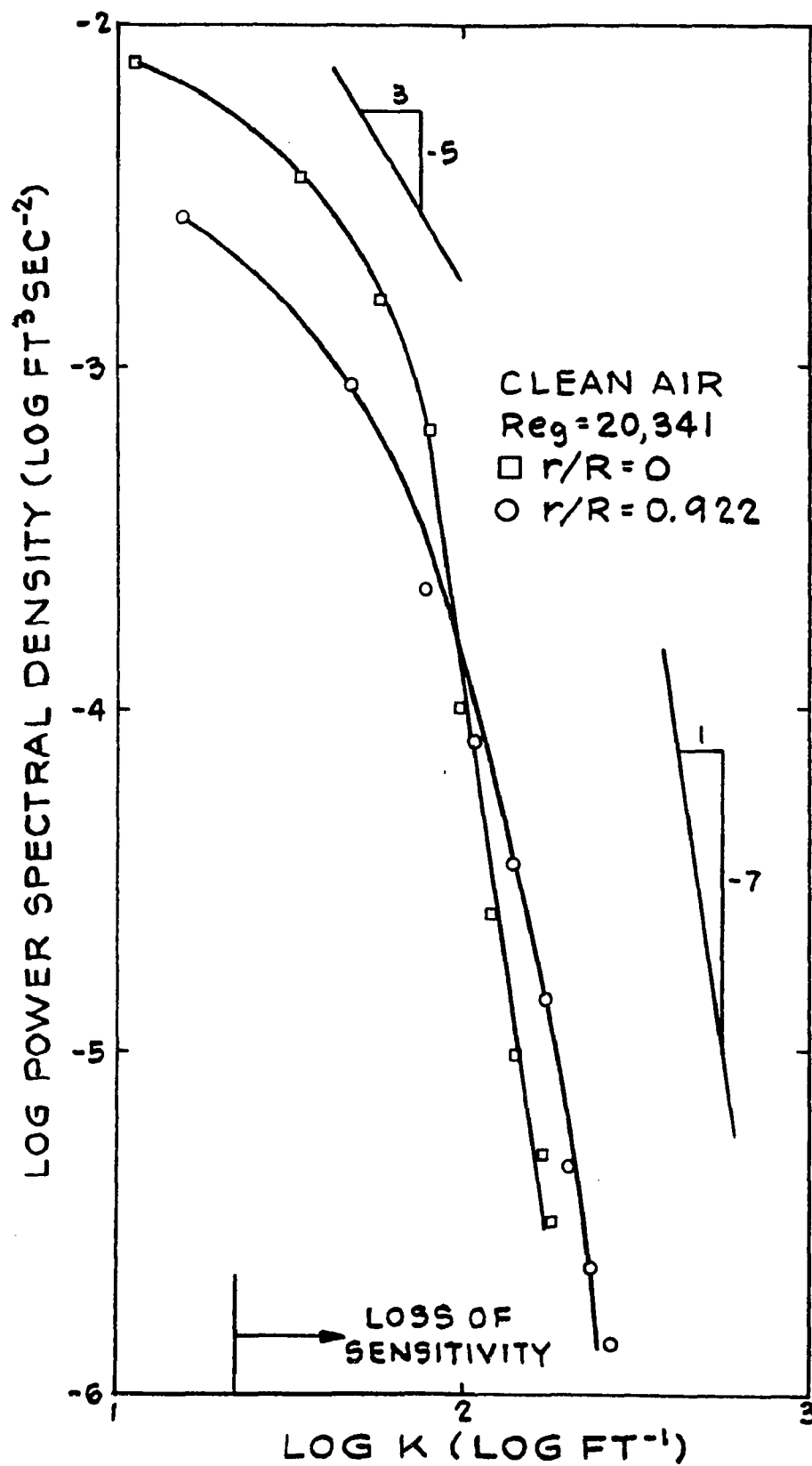


Figure 30
 Clean Air Spectrum Analyses

sensitivity, compared to that of the smaller hot-wire, had a much more serious affect on turbulent spectral measurements than on turbulence intensity measurements. Laufer also reported some crossover of spectral curves for different radial positions. Figure 30 shows a crossover; however, it appears in the doubtful range of wavenumbers.

The numerical values of energy density were significantly lower than the values reported by Laufer. However, these results were calculated from measurements at low Reynolds numbers and the Laufer results were calculated from measurements at high Reynolds numbers. No spectra taken at Reynolds numbers corresponding to this study have been reported in the literature.

CLEAN GAS NUSSELT NUMBERS

The clean gas heat transfer measurements were used to calculate Nusselt numbers for two gases, air and argon. The direction of the heat transfer and the instrumentation was different for the two test sections. Two different techniques were, therefore, used to calculate the gas side heat transfer coefficient and Nusselt number.

In the vertical test section, the heat input was calculated from:

$$Q = W_g c_{pg} (T_{OUT} - T_{IN}) \quad (35)$$

The heat input was equated to:

$$Q = UA(\bar{T}_w - \bar{T}_B) \quad (36)$$

where $\bar{T}_w$ was an average wall temperature weighted in accordance with the linear spacing of the 12 thermocouples along the heated section with UA given by:

$$UA = \frac{1}{\frac{t_{ss}}{k_{ss}A} + \frac{1}{h_i A_i}} \quad (37)$$

With the heat input and all the temperatures as known quantities, the gas side heat transfer coefficient was calculated directly.

In the horizontal test section, a counterflow heat exchanger equation was used to calculate UA. The gas side heat loss was calculated from:

$$Q = W_g c_{pg} (T_{IN} - T_{OUT}) \quad (38)$$

The heat loss was equated to:

$$Q = UALMTD \quad \text{where}$$

$$LMTD = \frac{(T_{OUT} - T_{WOUT}) - (T_{IN} - T_{WIN})}{\ln \left[\frac{T_{OUT} - T_{WOUT}}{T_{IN} - T_{WIN}} \right]} \quad (39)$$

$$\text{and } UA = \frac{1}{\frac{t_{ss}}{k_{ss}A} + \frac{1}{h_o A_o} + \frac{1}{h_i A_i}}$$

In operation the water temperature remained almost constant from the inlet to the outlet of the cooling jacket. The logarithmic temperature difference was almost exclusively dependent on variations in the gas temperature. The water flow rate was high enough that UA was sensitive only to the gas side coefficient. The resistance of the stainless steel tube was negligible in both test sections.

The Nusselt number was evaluated at the average bulk gas temperature. The results for air and argon are shown in Figure 31 where Nusselt number is plotted versus Reynolds number. The measured Nusselt numbers were about 10 percent

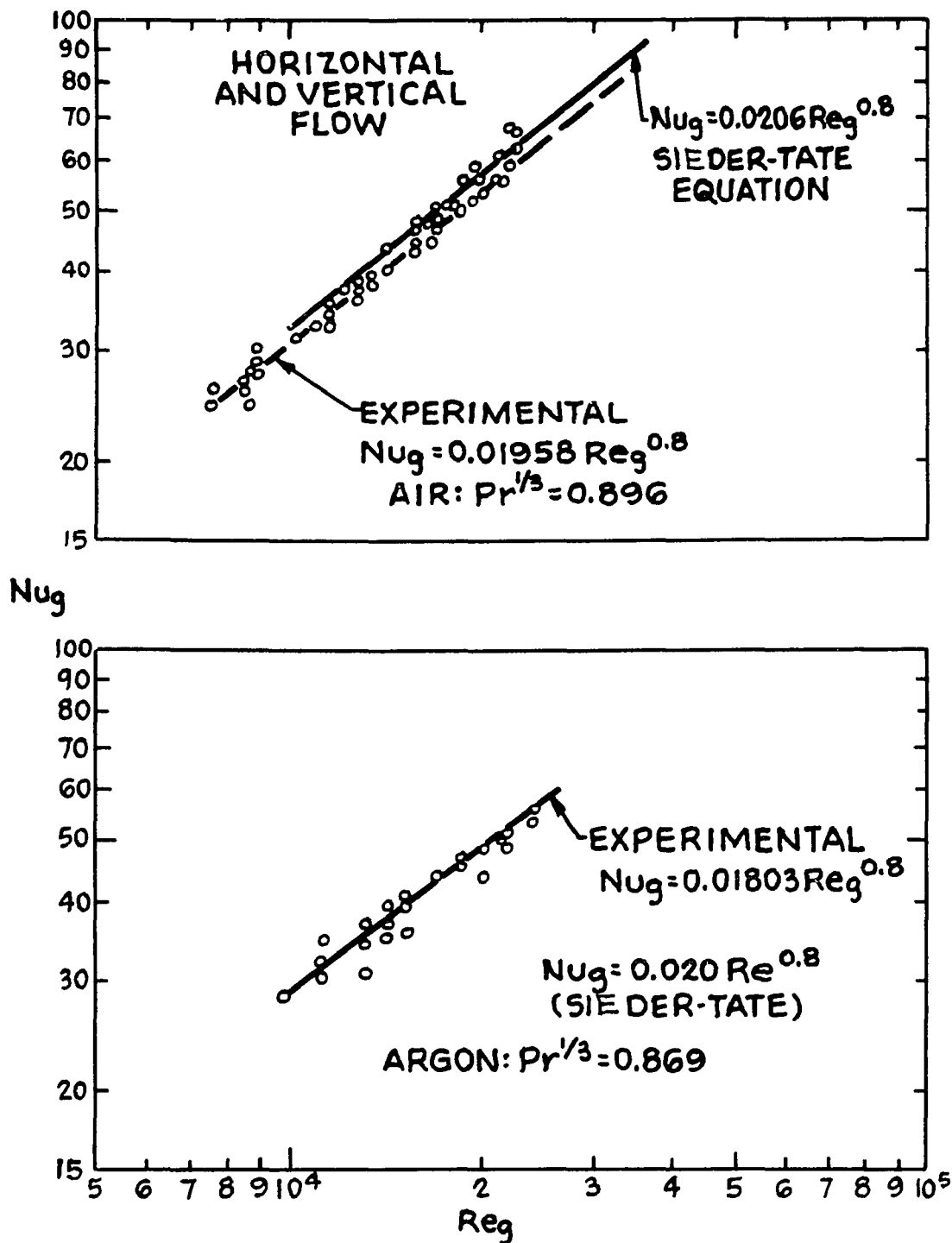


Figure 31

Clean Gas Nusselt Number Versus Reynolds Number

lower than the Nusselt numbers calculated from the Sieder-Tate equation which correlates experimental data within wide 30 percent limits (Kreith 1964:347). Prandtl number dependence was not an important factor because the one-third power of the Prandtl number for air and argon is essentially constant for the experimental range of temperatures. The clean gas values were the same in both the vertical and horizontal test sections and the lower horizontal cooling section as well. Clean gas Nusselt numbers were measured before and after suspension tests and were found to be unchanged.

Chapter 5

SUSPENSION RESULTS AND DISCUSSION

The results of the three phase experimental program confirmed the existence of drag and Nusselt number reduction in a gas-solids suspension. The profile measurements in particular provided corroboration of some theoretical conjectures and cast doubt upon some others. A detailed discussion of the viability of past theories of gas-solids drag reduction in light of the experimental results of this study is given in Chapter 6.

This chapter includes detailed description of the experimental results obtained with suspension flow and comparison, where applicable, to the results of other investigations. The chapter begins with some comments and observations on the behavior of the suspended particles in the closed loop. The rest of the chapter describes the results of the pressure drop, profile, and heat transfer measurements in suspension flow.

OBSERVATIONS ON FLOW AND SOLIDS ENTRAINMENT

Graphs of loading ratio versus weight added to the loop were developed for all particle types and gas Reynolds number ranges. These are shown in Figures 32, 33, 34, 35, 36, and 37 for the #980, #981, #279, #380, #660 glass beads and the

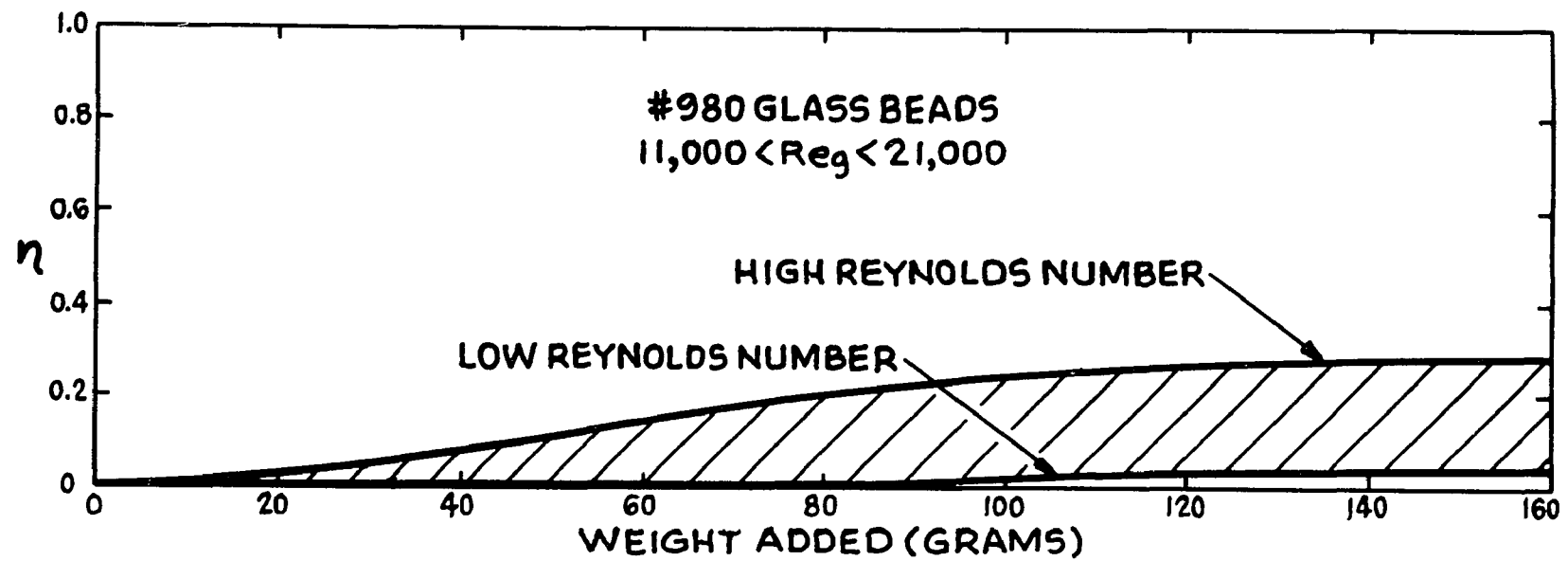


Figure 32 Loading Ratio Versus Weight Added--#980 Glass Beads

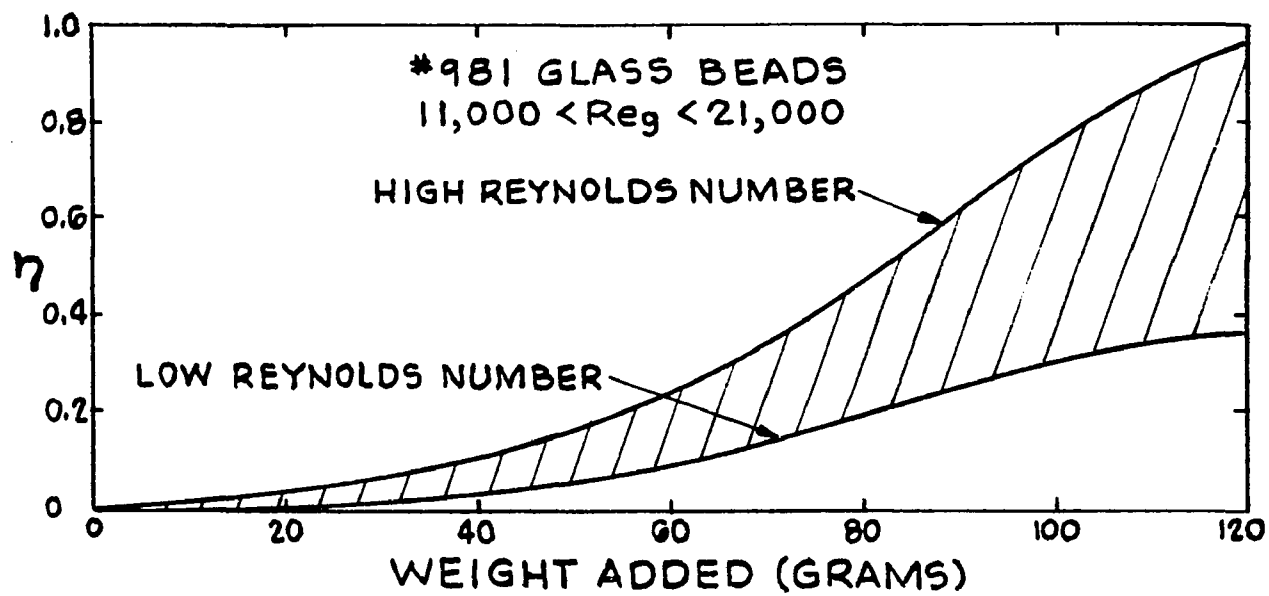


Figure 33 Loading Ratio Versus Weight Added--#981 Glass Beads

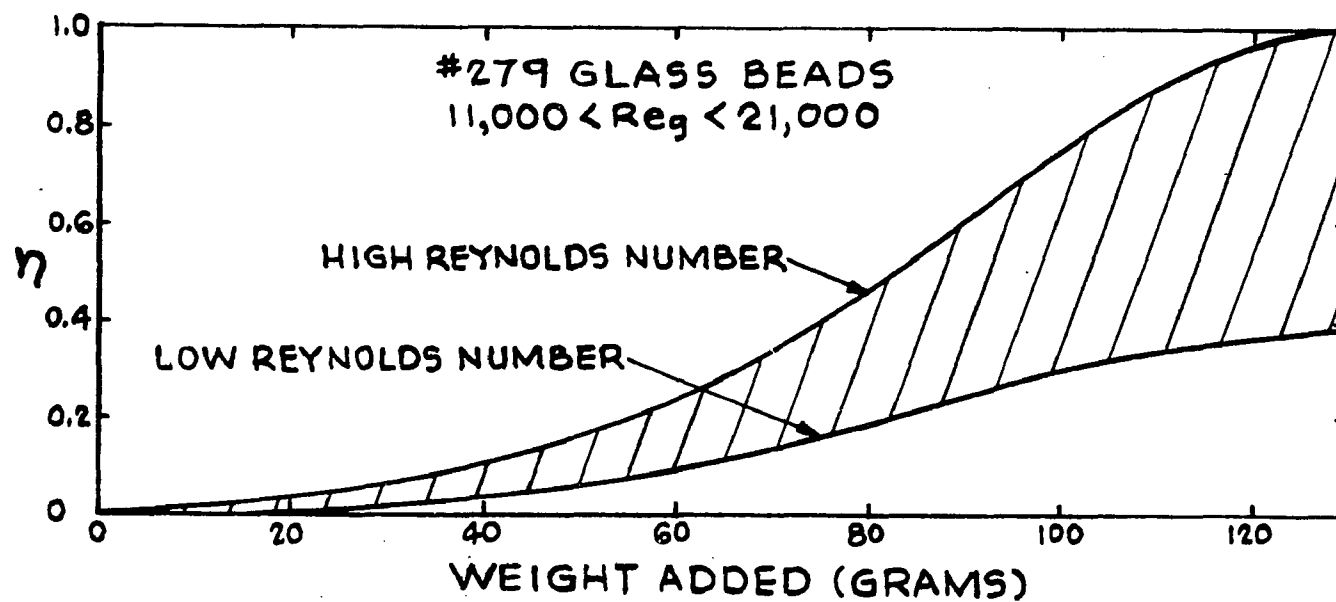


Figure 34 Loading Ratio Versus Weight Added--#279 Glass Beads

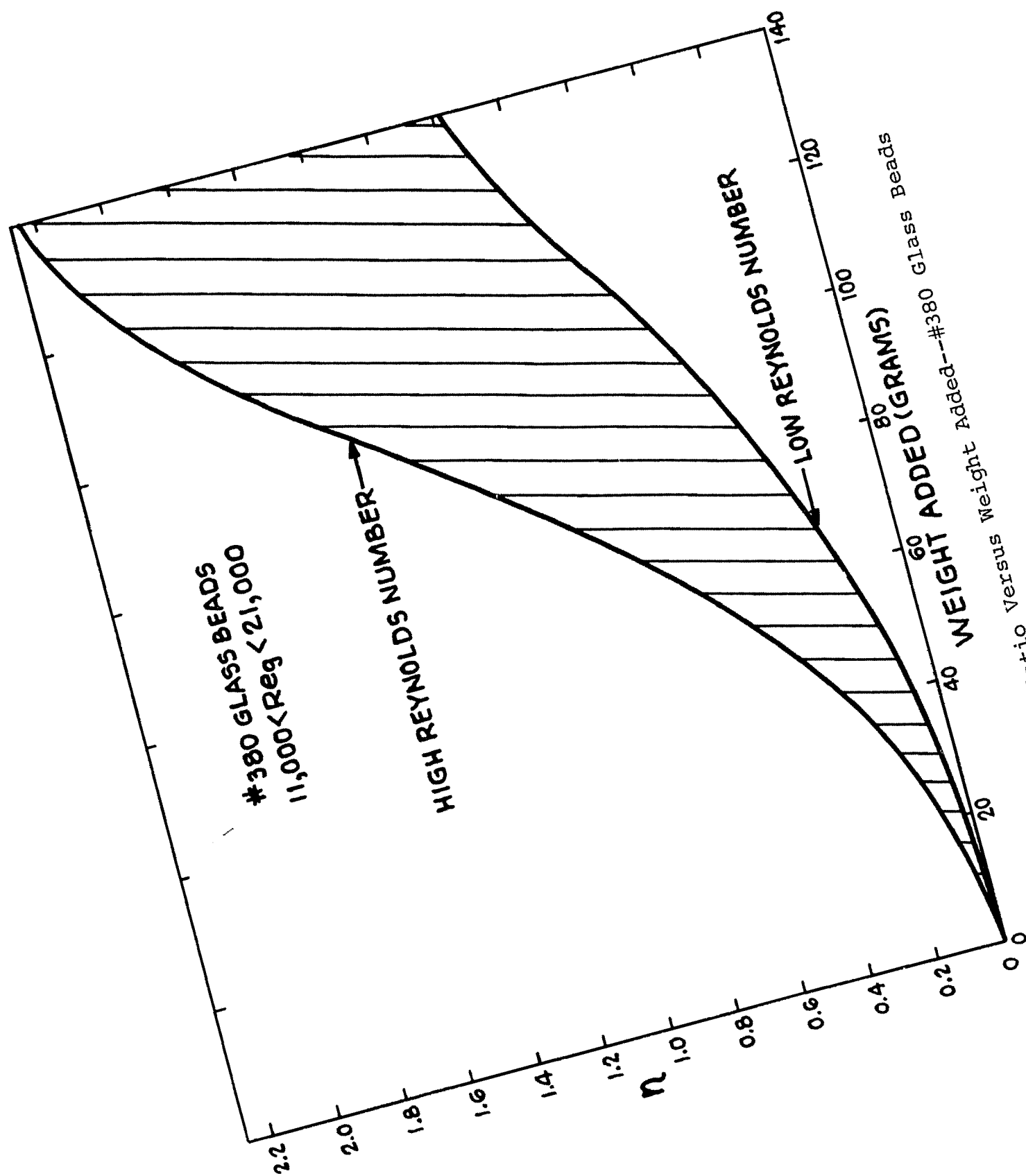


Figure 35 Loading Ratio Versus Weight Added--#380 Glass Beads

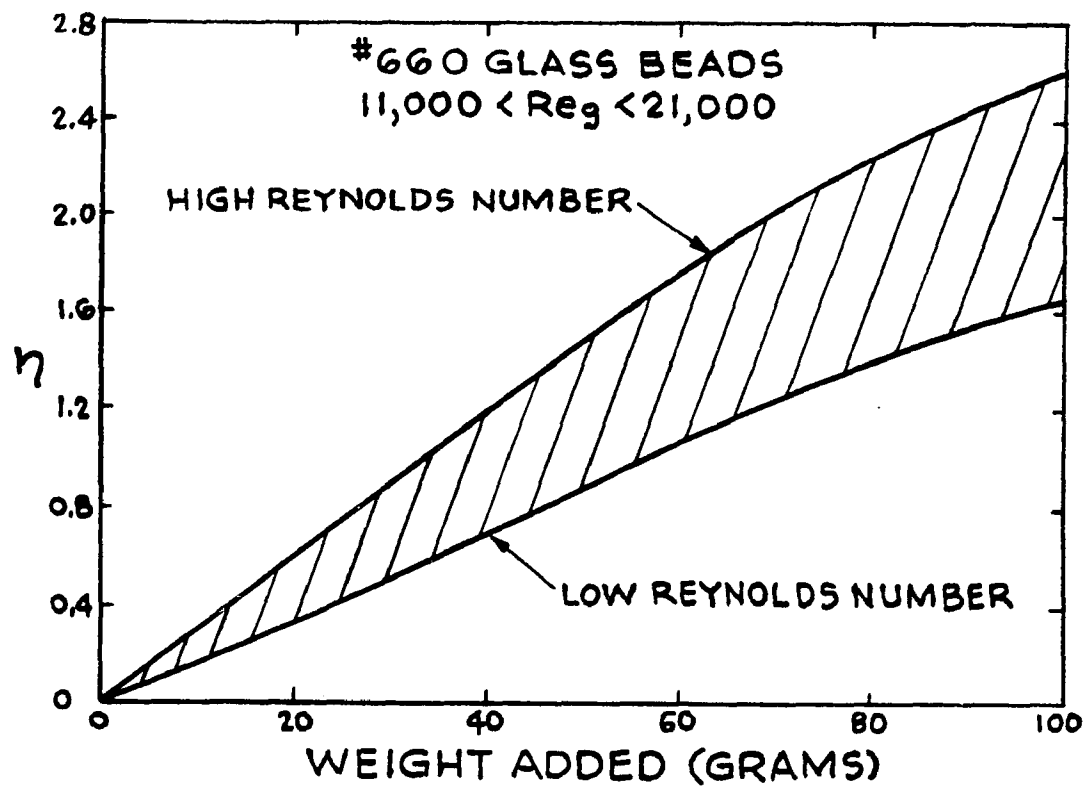


Figure 36 Loading Ratio Versus Weight Added--#660 Glass Beads

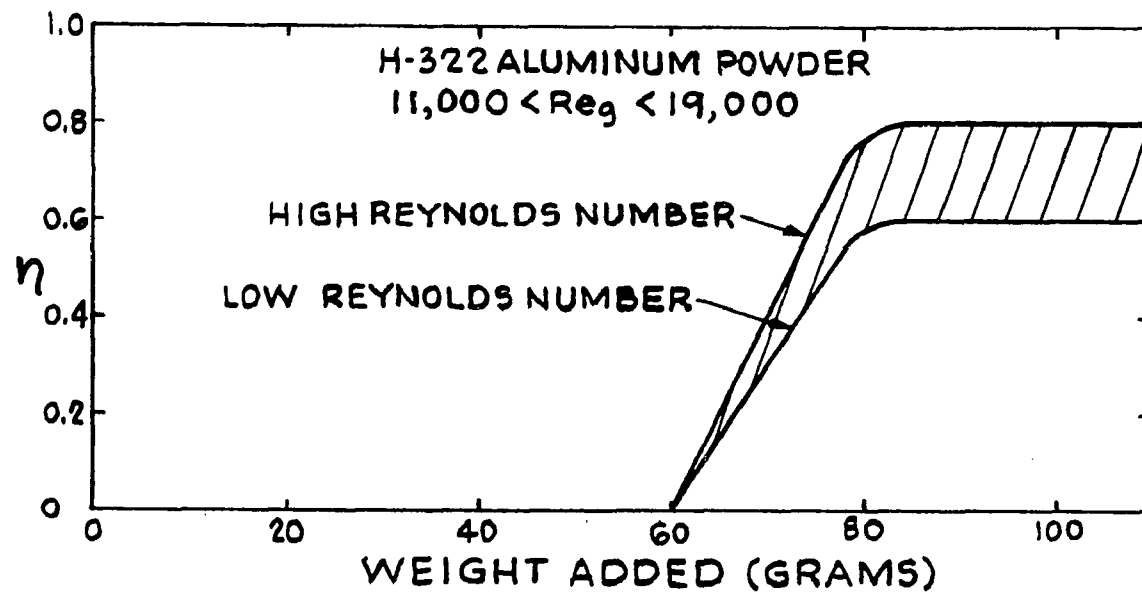


Figure 37 Loading Ratio Versus Weight Added--H-322 Aluminum Powder

H-322 aluminum powder respectively. With the exception of the graph for the H-322 aluminum powder, the figures refer to the conveyance of solids in air. The figure for the H-322 aluminum powder refers to the conveyance of solids in argon.

The graphs were plotted from data accumulated during selected runs following thorough loop cleanings but at varied ambient conditions. It was apparent from these figures and additional data (not shown) that the exact value of the loading ratio corresponding to a particular weight of solids added depended on a multitude of factors. The most important of these factors were the particle size, gas Reynolds number, ambient humidity, and the amount of particles remaining in dead spots after a loop cleanout. The effect of the first two factors can be seen qualitatively from the figures. The effect of the last two was even more qualitative and could not be seen on the figures.

Large particles permitted circulation of higher loading ratios than smaller particle sizes. For example, adding 100 grams of #380 glass beads to a clean loop resulted in an average loading ratio of about 1.0 whereas adding the same amount of #980 glass beads to a clean loop resulted in a loading ratio of about 0.1, a full order of magnitude difference. The larger Reynolds numbers also permitted circulation at higher loading ratios, although the Reynolds number effect was not as important or as consistent as the particle size effect. The proportionate spread of loading ratio at a particular value of weight added was roughly equal to the proportionate spread in gas Reynolds numbers.

The shapes of the loading ratio curves were similar for the #981, #279, and #380 glass beads. For each of these three particle designations, the first 40 grams added produced

little entrainment. The next 60 grams added produced significant entrainment. Additional weight added over 100 grams did not consistently increase entrainment except at the highest Reynolds number. Unfortunately, the circulator could not sustain the highest Reynolds numbers with large quantities of solids in the loop because of excessive power requirements. Therefore, adding more than 140 grams often did not increase the loading ratio and occasionally decreased the loading ratio with the appearance of saltation in the horizontal sections.

The shape of the loading ratio curve for the #660 glass beads differed from the ones discussed above only at low loading ratios. The first 40 grams of weight added did produce significant entrainment although the same tendency of the curves to level off at high values of weight added appeared. A certain amount of caution must be introduced in evaluating this particular curve because of the unknown effect of the attrition of the #660 beads on the target-meter response. The Brush recorder reading did not appear to change with time as the #660 glass bead suspension circulated and as the particles attrited. An increasing proportion of fine particles would have been expected to increase the reading with time. Alternatively an increasing proportion of fine particles would have contributed to a loss of entrainment by particle deposition. The two effects apparently offset each other in terms of the recorder reading. However, there is no way of assessing the effect of the particle attrition upon the target-meter calibration. The open loop calibration did not account for attrition since particles passed only once through the circulator before collection and did not have time to undergo significant diameter changes.

The loading ratio curve for the #980 glass beads was much flatter than for the other particles indicating the great difficulty encountered in circulating fine particles. The difficulty was attributed to the importance of electrostatic charging and cohesion upon the behavior of small particles. Both conditions are more important in suspensions of small particles than large ones because both conditions are aided by increased contact and surface area (Boothroyd 1971:34, 213). With the exception of the H-322 aluminum powder, only the #980 glass beads in suspension gave obvious evidence of electrostatic deposition in the form of herringbone patterns on the walls of the pyrex viewing sections. Further addition of particles at first removed the deposits but then they reformed and became almost impossible to remove. Similar phenomena have been observed by Boothroyd (Boothroyd 1971:138), Wachtell and Waggener (Wachtell and Waggener 1964:14), and Rossetti (Rossetti 1969:187). The cohesiveness of the various particle sizes was obvious from preliminary screening of the particles before addition to the loop. The #660 and #380 glass beads poured through the screen while the #279 and #981 glass beads required some mechanical aid to pass through. However, the #980 glass beads, particularly when the laboratory humidity was higher than normal, almost had to be forced through the same screen.

The loading ratio curve for the H-322 aluminum powder was quite different than any of the curves for the glass beads. The first 80 grams added did not produce any sustained entrainment. The Brush recorder reading rose initially to moderate values but then dropped to the equivalent clean air reading as all of the aluminum deposited or plated out on the loop. The next 20 or more grams produced a

constant loading ratio of about 0.70 regardless of how much more than 80 grams was added. Apparently the first 80 grams filled dead spots in the loop and provided an initial coating on tube walls. Particles added after 80 grams deposited on the walls at the same rate as other particles reentrained.

The form of the aluminum deposit on the viewing sections differed from the ones observed during circulation of the #980 glass beads. Instead of a definite geometrical pattern, the deposit appeared as a uniform darkening of the initially clear pyrex glass. The deposit could not be removed even by blasting with a high pressure air hose. Examination of protruberances into the flow channel revealed a rock-hard buildup of aluminum on any surface subject to direct bombardment. Very heavy accumulations were noted on thermocouples, the target meter, and the nose-cone of the circulator. Figure 38 shows the type of deposit that was found on all thermocouples after bombardment by aluminum powder. (The uniformity of the deposit on this thermocouple removed from the vertical heat transfer test section suggested that the aluminum powder was uniformly dispersed across the vertical tube diameter.) The propensity of the aluminum powder to bond so solidly to the loop walls and equipment may have been the result of its wide size distribution. Although the aluminum powder averaged 30.0μ diameter by weight, the powder included particle sizes ranging from 5.8 to 55.0μ diameter. Boothroyd (Boothroyd 1971:140) maintains that a polydisperse powder will afford a much greater number of additional cohesive contact points than a narrowly sized powder. The aluminum powder welded itself to metallic objects by plastically deforming on collision with initial non-uniform deposits formed by other means. The other means of deposition could have been

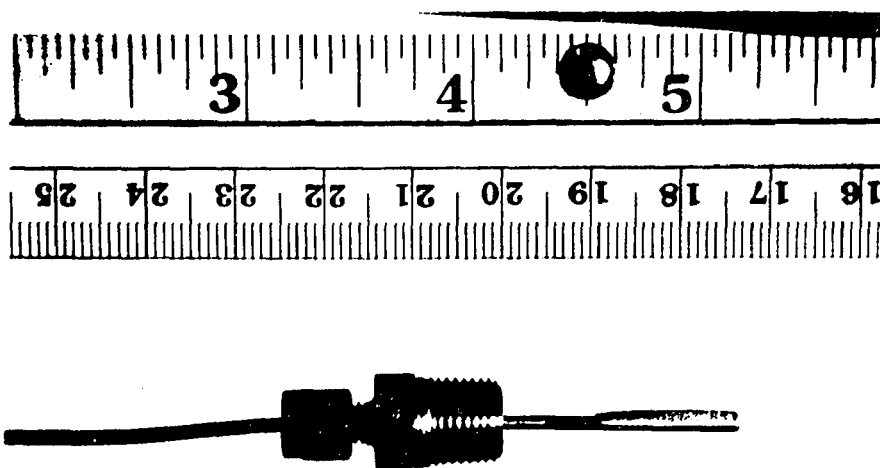


Figure 38
Aluminum Deposit on a Thermocouple

caused by electrostatic attraction, gravity sedimentation, or thermophoresis.

All of the particle types were subject to some form of deposition at isolated locations in the loop. The one common property of all the deposit locations was that they represented a discontinuity of some physical loop characteristic. The first type of discontinuity was the least serious: the locations where the stainless steel charge leakage path was interrupted by the non-conducting pyrex viewing sections. Only the #980 glass beads and the H-322 aluminum powder formed uniform deposits on the pyrex viewing sections.

Other particle designations deposited on the lower wall of the horizontal pyrex viewing section. However, this type of deposit could be considered as occurring at a second type of discontinuity: the locations where the flow expanded in cross sectional area producing a sudden decrease in velocity. Two mechanisms contributed to deposition at flow expansions and disturbances. In horizontal sections, large agglomerates of small particles fell out of suspension as the flow velocity decreased. In vertical sections, smaller particles tended to follow eddy patterns caused by flow disturbances and deposit in dead spots. This phenomenon was most apparent at the target-meter location where heavy deposits, as much as 40 grams, of small particles were often found filling the tube containing the target meter support. Large individual particles were less prone to leave the main flow pattern.

The third type of discontinuity, a thermal one, was represented by the location of the water cooling jackets. Thermophoresis apparently caused the finer particles to plate out on the cool walls of the jacketed tubing. This type of

deposit became severe after installation of the two additional water cooling jackets for the heat transfer measurements.

The ability of a particle to deposit at any of the three types of discontinuities increased with decreasing particle size. The loading ratio curves reflected this ability. Partially because the amount of deposition could not be controlled, the loading ratio curves did not provide information to accurately predict the amount of entrainment. Only the target-meter response gave reliable indications of the loading ratio for a given run.

The two other factors that were not incorporated into the loading ratio curves also prevented use of the curves as calibrations. The laboratory relative humidity while usually less than 50%, was not regulated but appeared to have a marked effect on the cohesiveness of the particles. The amount of particles remaining in dead spots after a loop cleanout was also not controlled. When runs with a designated particle were in progress, the loop cleanout procedure did not include removal of particles that had accumulated in dead spots such as the three-way ball valve body cavity, the tube holding the target-meter, and the circulator shaft bearings. Only when particle sizes were changed or when clean air data was required was every trace of particle accumulation removed with the high pressure airhose and an industrial vacuum cleaner. In general, less than thorough cleanings permitted higher loading ratios.

After prolonged circulation in the closed loop, it was possible to differentiate the various glass bead particle designations by their color. The glass beads before circulation were white. After circulation their color ranged from

an off-white to a dark gray with increasing darkness corresponding to their nominal size before circulation. The color of the used #660 glass beads suggested that bits of eroded stainless steel were mixed with the bead sample. The likely source of the eroded stainless steel was the pump impeller and impeller housing.

SUSPENSION FRICTION FACTORS

Unlike the clean air pressure drop measurements, suspension pressure drop measurements involved a correction for static head in the vertical test section in order to determine the frictional pressure drop. In the general case the pressure drop across a test section containing a gas-solids suspension is:

$$\Delta P = \Delta P_{ag} + \Delta P_{ads} + \Delta P_{Lg} + \Delta P_{Lds} + \Delta P_{fg} + \Delta P_{fds} \quad (40)$$

The pressure drop caused by acceleration of gas was negligible because the flow was fully developed and isothermal in both test sections. The pressure drop caused by acceleration of the solids was also negligible because the bulk slip velocity, as reported in a later section of this chapter, was small and the solids acceleration pressure drop could be related directly to the gas acceleration pressure drop (Boothroyd 1971:127):

$$\Delta P_{ads} \cong \frac{W_p}{W_g} \Delta P_{ag} \quad (41)$$

The static head of air was automatically balanced by the physical configuration of the micromanometer. However, the static head of solids in the vertical test section had to be

subtracted from the reading of the micromanometer in order to determine the frictional pressure drop. The loading ratio was assumed to be the same as the ratio of the dispersed solids density to the air density. Therefore:

$$\Delta P_{Lds} = \rho_{ds} \frac{gL}{g_c} = \eta \rho \frac{gL}{g_c} \quad (42)$$

was assumed. This would be true only if the bulk slip velocity between the gas and solids was negligible which was verified later by the particle velocity profile measurements.

The pressure drop caused by fluid friction and by solids friction could not be differentiated by the micromanometer so that both were combined into one frictional term:

$$\Delta P_f = \Delta P_{fg} + \Delta P_{fds} \quad (43)$$

The frictional pressure drops were used to calculate friction factors from the equation:

$$f_s = \frac{2\Delta P_f g_c D}{\rho U^2 L} \quad (44)$$

The air density was used in the friction factor calculations in order to provide a direct comparison between clean air and suspension factors.

Vertical Test Section

The results of the pressure drop measurements are shown in Figure 39 for the #980, #981, and #279 glass beads in the vertical test section and in Figures 40 and 41 for the #380 and #660 glass, respectively in the vertical test section. The graphs were plotted as the ratio of the suspension friction factor to the clean gas friction factor versus

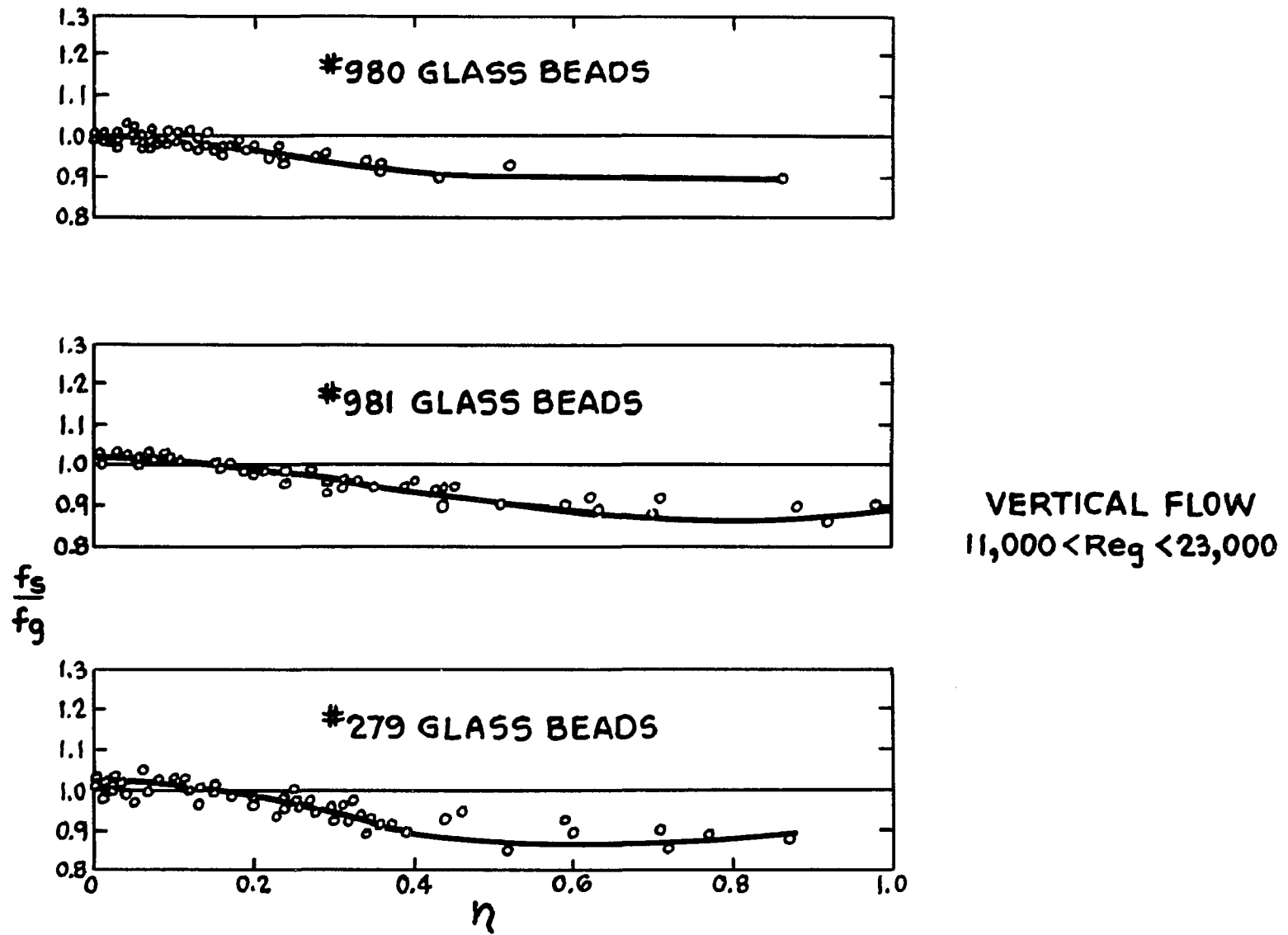


Figure 39 Vertical Friction Factor Ratio Versus Loading Ratio--
#980, #981, and #279 Glass Beads

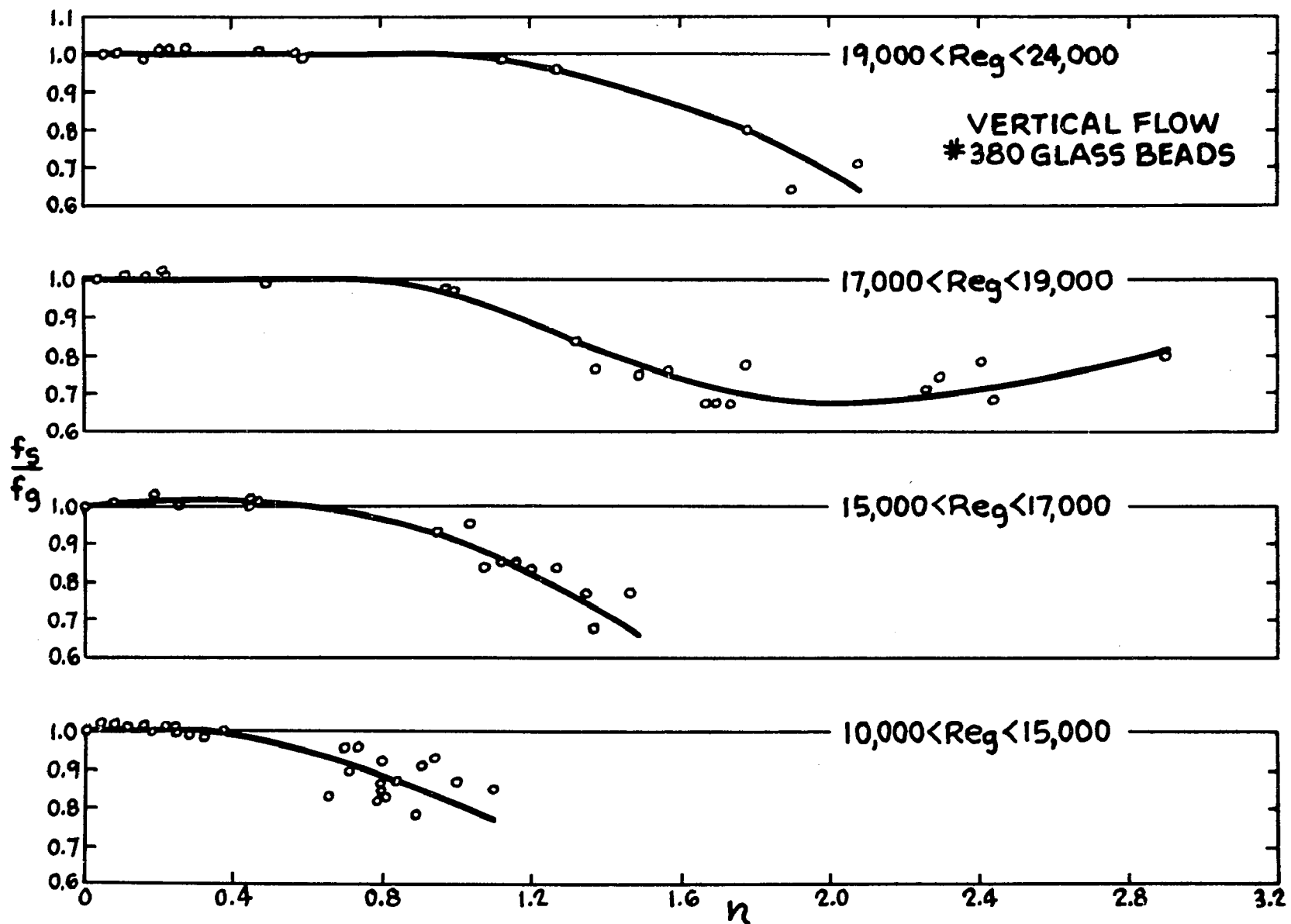


Figure 40 Vertical Friction Factor Ratio Versus Loading Ratio--#380 Glass Beads

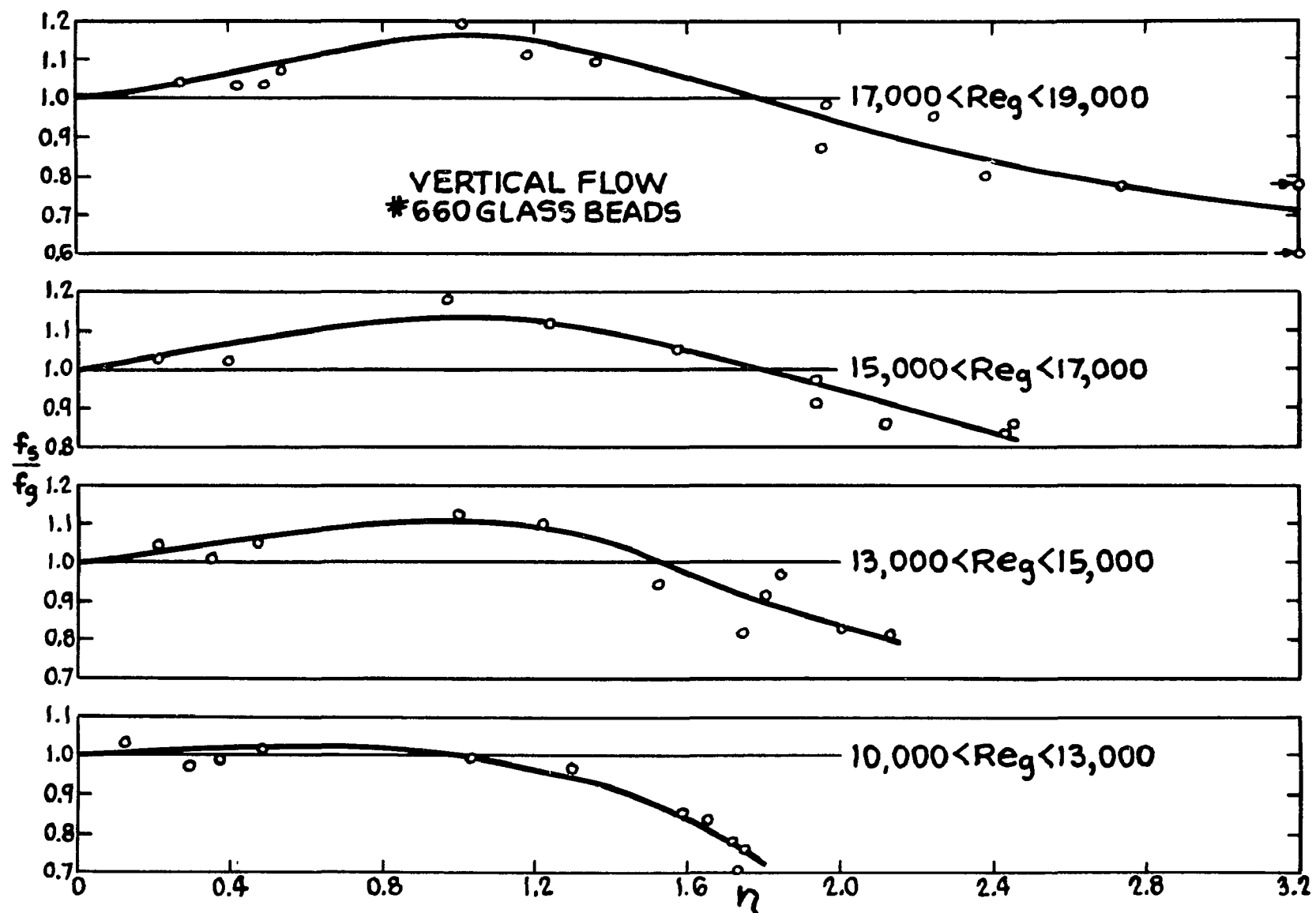


Figure 41 Vertical Friction Factor Ratio Versus Loading Ratio--#660 Glass Beads

loading ratio at corresponding gas Reynolds numbers. Drag reduction was indicated by values of the friction factor ratio below unity.

Drag reduction was achieved for all five glass bead designations in the vertical test section. The maximum drag reduction was about 40% for both the #380 and #660 glass beads. The onset of drag reduction with loading ratio was progressively delayed with increasing particle size. For the #380 and #660 glass beads, which showed a variation of drag reduction with gas Reynolds number, the onset of drag reduction was also delayed with increasing gas Reynolds number. Only the friction factor ratio curve for the #380 glass beads at a gas Reynolds number of about 18,000, showed a distinct minimum. All other curves, after the onset of drag reduction, either showed a monotonic decrease of friction factor ratio or a decrease to a uniform level of drag reduction. In general the friction factor ratio remained near unity until the onset of drag reduction. However, the #660 glass beads showed a sizable drag increase before the onset of drag reduction. As with the loading ratio curve for this particle size, some caution must be introduced into any conclusions drawn from the results obtained with the #660 beads because of significant particle attrition.

The results for the #980, #981, and #279 glass beads were very similar although the maximum amount of drag reduction for the #980 glass beads was slightly smaller at 10% compared to 13% achieved for the #981 and #279 glass beads. As expected the results for the #981 and #279 glass beads were almost identical. The onset of drag reduction

appeared at a slightly lower loading ratio (0.10) for the #980 glass beads than for the #981 and #279 glass beads (0.15). The friction factor ratio curves for these particle designations showed a decrease to a moderate uniform level of drag reduction but no distinct minimum. The slight drag increase, 2%, at very low loading ratios for the #981 and #279 glass beads was probably caused by a combination of solids friction and additional tube roughness produced by a small amount of particle deposition.

The flow of suspensions of #380 glass beads produced drag reductions of as much as 36%. The onset of drag reduction was delayed until loading ratios of 0.2 to 1.0 depending on the gas Reynolds number. After the onset of drag reduction, decrease in friction factor ratio was monotonic at all Reynolds numbers except at a Reynolds number of about 18,000 where a distinct minimum at a loading ratio of 2.0 was achieved. At this Reynolds number and for this particle designation, the pump was able to circulate high loading ratios without significant loss of entrainment. A distinct minimum would probably have been achieved for all the other curves and particle designations if high enough loading ratios could have been attained. Before the onset of drag reduction, the friction factor ratio remained at or slightly above unity.

For the #660 glass beads, drag reduction of as much as 39% was achieved. The onset of drag reduction was further delayed until loading ratios of 1.0 to 1.8 depending on the gas Reynolds numbers. After the onset of drag reduction, the decrease in friction factor ratio was generally monotonic. The data for Reynolds numbers of about 18,000 suggested that a minimum similar to that of the #380 glass beads

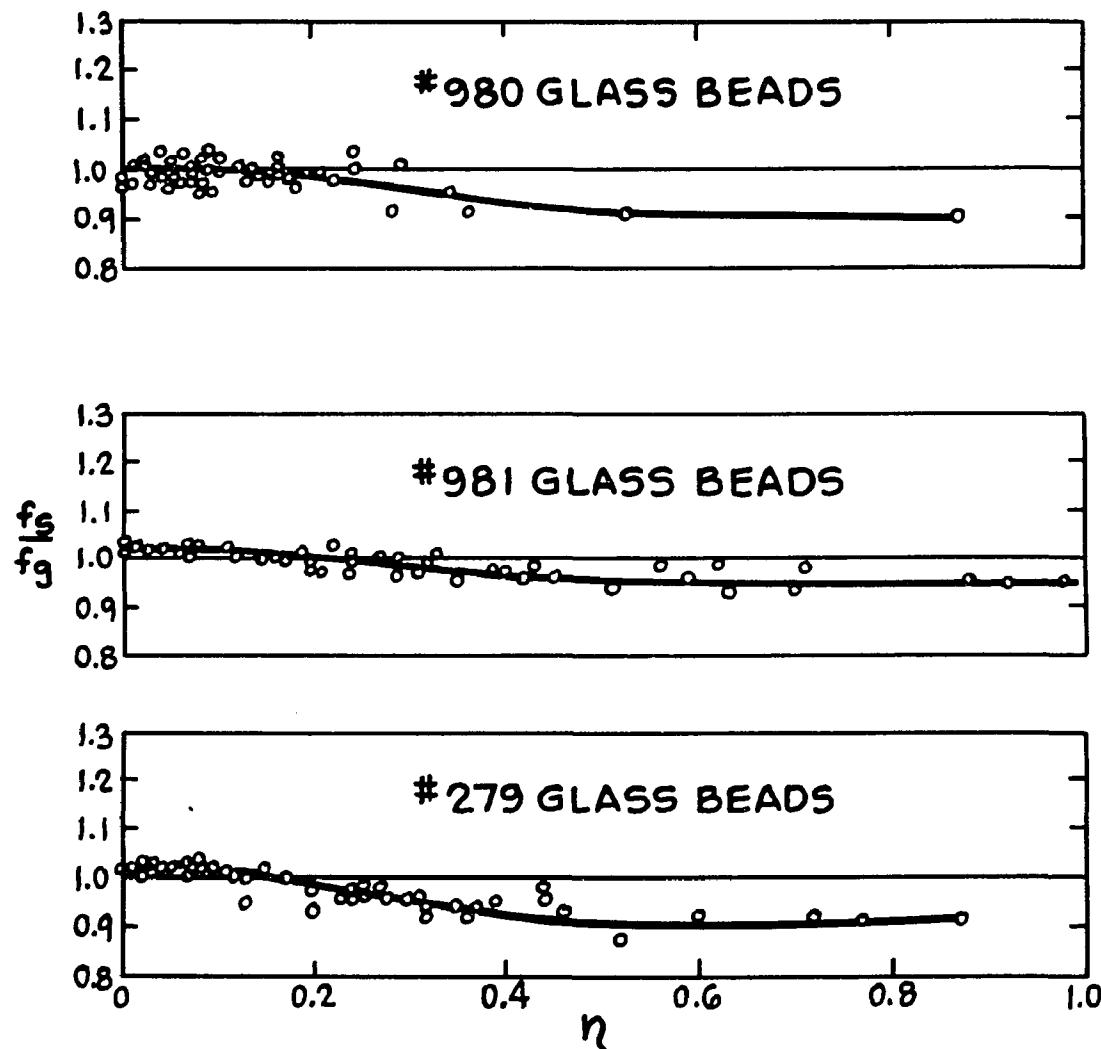
had been achieved. However, an insufficient number of experimental points could be obtained at these high loading ratios to conclusively define the minimum location. Again the results for this particle designation are clouded by the unknown effects of particle attrition.

Horizontal Test Section

The results of the pressure drop measurements are shown in Figure 42 for the #980, #981, and #279 glass beads in the horizontal test section and in Figure 43 for the #380 and #660 glass beads respectively in the horizontal test section. The results were almost identical to the results obtained in the vertical test section for the three smallest glass bead sizes. The #380 and #660 glass bead suspensions yielded drag increases rather than drag reduction in the horizontal test section. Apparently gravity segregation and sedimentation had a negligible effect upon the drag reducing ability of the smaller particle sizes but had a detrimental effect upon that of the larger particle sizes. The maximum drag increase for the #380 glass beads was 21%; for the #660 glass beads it was 44%. No Reynolds number effect was discernible for any of the five glass bead designations. The difference in results for the two test sections indicated that uniform particle distribution was important for the existence of drag reduction.

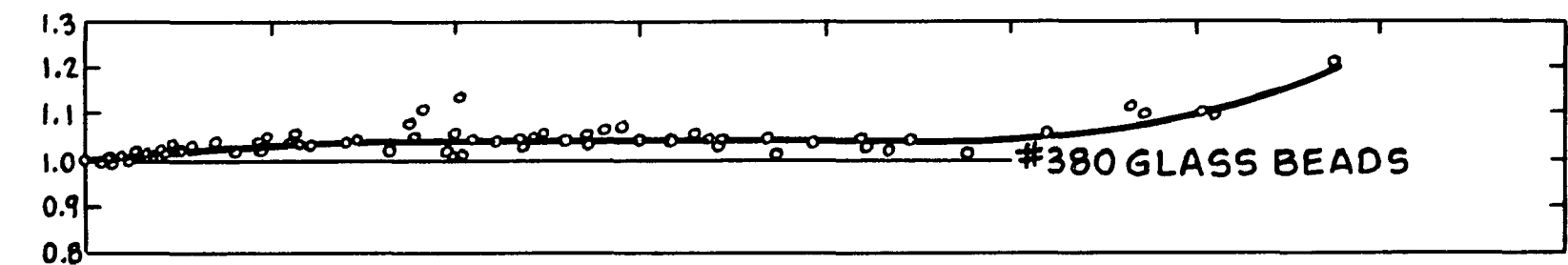
Comparison to Results of Other Investigations

While several earlier investigations have measured drag reduction in gas-solids suspensions, only one reported data for the range of Reynolds numbers and particles sizes considered in this study. Rossetti (Rossetti 1969) using the identical mass flowmeter and circulator and a similar closed



HORIZONTAL FLOW
 $11,000 < Re_g < 23,000$

Figure 42 Horizontal Friction Factor Ratio Versus Loading Ratio--
 #980, #981, and #279 Glass Beads



$\frac{f_s}{f_g}$

HORIZONTAL FLOW
 $11,000 < Re_g < 23,000$

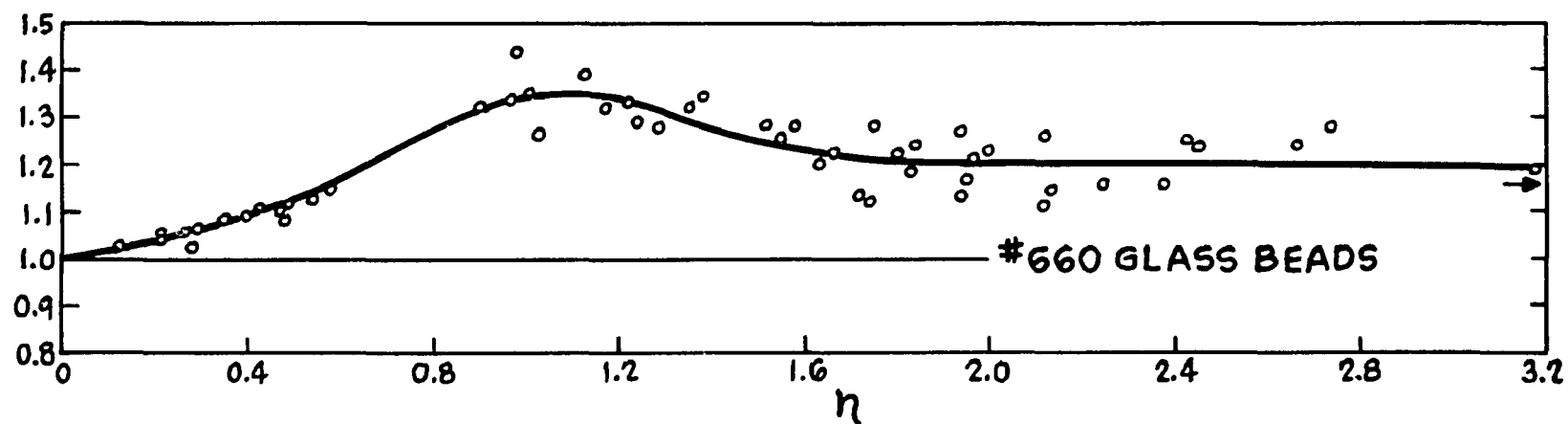


Figure 43 Horizontal Friction Factor Ratio Versus Loading Ratio--#380 Glass Beads and #660 Glass Beads

loop system also measured drag reduction in vertical and horizontal test sections at Reynolds numbers between 10,000 and 25,000 and particle sizes between 10 and 60 μ . The particles used were the same Superbrite glass bead designations, #980, #981, #279, #380, and #660.

The trends of the Rossetti results were almost identical to the ones reported here. Drag reduction was achieved for suspensions of all five particle designations in the vertical test section and for suspensions of all but the #380 and #660 glass beads in the horizontal section. The maximum drag reduction was achieved for the #380 and #660 glass beads in the vertical test section. The #980, #981, and #279 glass bead suspensions gave similar results in both the vertical and horizontal test sections. Drag increases were noted for the #380 and #660 glass beads in the horizontal test section.

There was a significant difference in the numerical results of the Rossetti study and the results of this study, especially in the vertical test section, because Rossetti did not establish target-meter calibrations for the different particle sizes and applied his loading-ratio calibration for the #380 glass beads to all glass bead designations. The loading ratio appeared in the calculations of the frictional pressure drop in the vertical test section and strongly affected the amount of drag reduction. A further cause of difference between the Rossetti results and the ones reported here was the difference in the size and material of construction of the two vertical test sections. Rossetti worked with a 1 inch I.D. pyrex pipe following a 0.870 inch I.D. stainless steel entrance region. The vertical test section here was a 0.870 inch I.D. stainless steel tube preceded by a long length of identical 0.870 inch I.D. stainless steel

tubing. Numerical comparison of the vertical test section results is obviously inappropriate. However, the results of the two studies implied that test section electrical characteristics have no effect on the existence of gas-solids drag reduction.

The horizontal test sections were fabricated from identical material in the two studies; and numerical comparison between the two studies is possible in a limited context. It must be assumed for the purposes of comparison that the maximum loading ratios achieved by Rossetti were somewhat higher than the ones achieved here because Rossetti was able to attain higher Reynolds numbers. The loop used by Rossetti was shorter than this one and had a lower total frictional resistance to flow. It could, therefore, sustain higher air flow rates and better solids entrainment.

The maximum amount of drag reduction reported by Rossetti in the horizontal test section for the #980, #981, and #279 glass beads only slightly exceeded the amount reported in this study. The maximum amount of drag increase, reported by Rossetti, in the horizontal test section for the #380 and #660 glass beads almost exactly corresponded to the amount reported in this study. The shape of the friction factor ratio curves for the #380 glass beads were very similar for both studies.

Rossetti did not report distinct minima: however, three other investigators have reported the appearance of distinct minima at loading ratios between 1.0 and 2.0. The three investigations (Soo 1967:144; Boothroyd 1966; Boyce and Blick 1970) were conducted at a variety of Reynolds numbers, tube sizes, and particle diameters and could not be used for direct numerical comparison with the results of this study.

The amount of reported maximum drag reduction varied from 5% (Soo) to 70% (Boothroyd). However, there appeared to be uniform agreement that an optimum loading ratio existed for drag reduction. The fact that the optimum occurred in a dilute suspension emphasized the danger in extrapolating results of dense phase tests to describe the behavior of dilute suspensions. The wide variety of experimental apparatus and material used in other investigations and the painstaking care taken in this investigation to eliminate experimental assumptions proved conclusively that gas-solids drag reduction is a real phenomenon and is not a result of questionable data reduction and experimental techniques.

SUSPENSION AIR VELOCITY PROFILES

Thirty air velocity profiles were measured for a wide variety of suspension conditions. All thirty profiles were similar to the corresponding clean air velocity profiles. Figure 44 shows the results of the two profiles taken near the maximum drag reducing conditions for the #380 and #279 glass beads. The suspension data have been superimposed on the curve fitting the clean air velocity profile data. The small difference between the suspension data points and the clean air curve was within the limits of scatter of the clean air velocity profile data. This figure confirmed the assumption concerning the ratio between the gas average velocity and the gas centerline velocity for suspension flow.

Three investigations have been reported in the literature which include measurement of air velocity profiles in the presence of dilute loadings (loading ratios less than 2.5) of small (less than 100μ diameter) particles. They

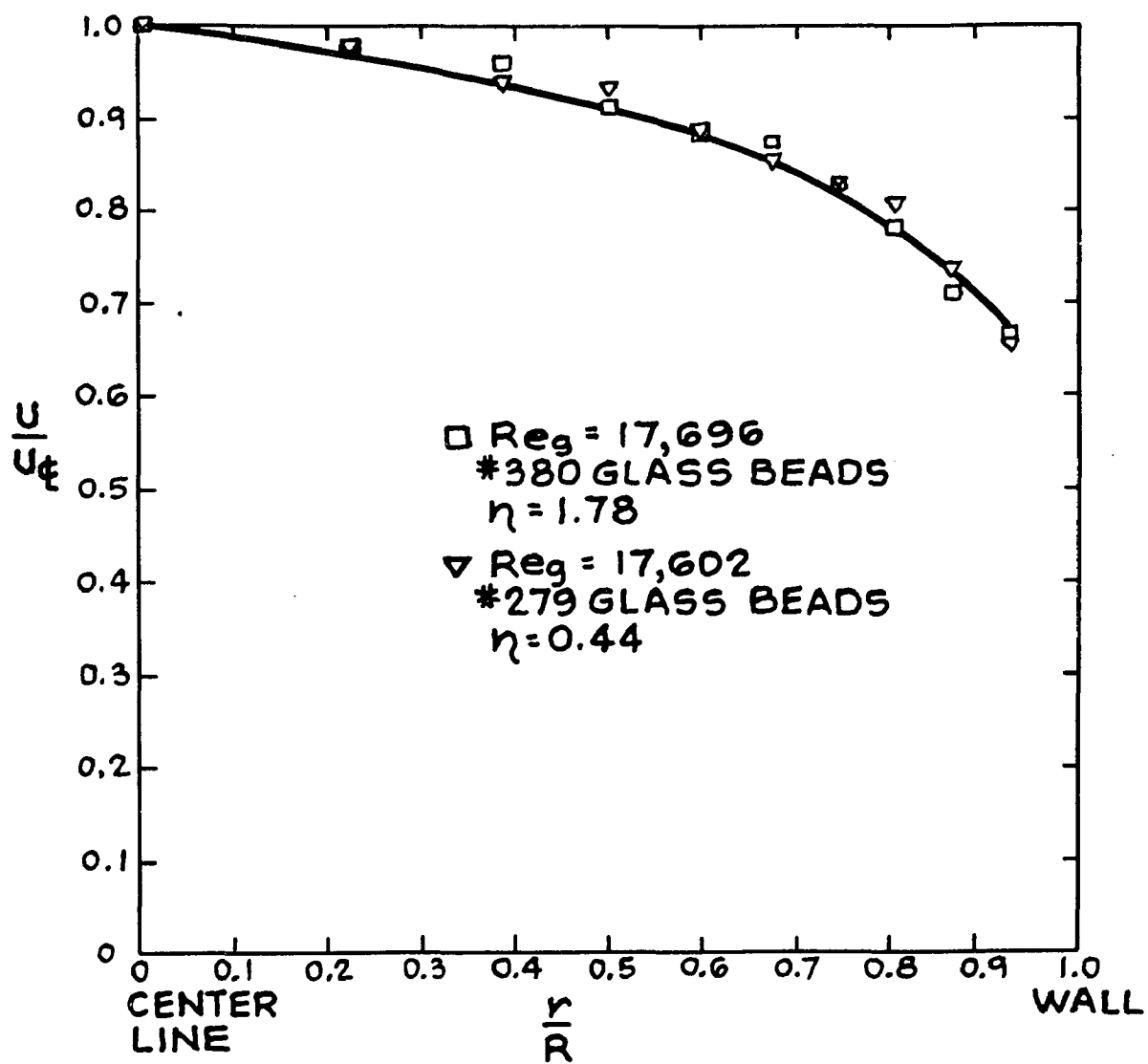


Figure 44
Suspension Air Velocity Profiles

all reported (Peskin and Dwyer 1964; Soo, Trezek, Dimick, and Hohnstreiter 1964; Kramer and Depew 1972a) that air velocity profiles were essentially unchanged from clean air profiles. The earlier measurements were taken with pitot tubes. This study provided the first confirmation of the previous results using a different measurement technique, fiber-film anemometry.

Figure 45 shows the two profiles for the #380 and #279 glass beads replotted in terms of the friction velocity and the universal velocity profile. The curve fitting the clean air data has been shown for comparison. The universal suspension profiles for drag reducing conditions fall above the profiles for the clean air conditions. The general shape of the profiles were the same for all cases. However, the deviation of the profiles below a logarithmic law begins sooner for the suspension profiles than for the clean gas profiles. The implication drawn from the results was that the viscous sublayer and buffer layer profiles extended further from the wall in the drag reducing suspension than in the clean gas. In other words, the viscous sublayer and the buffer layer had thickened.

Both Soo (Soo 1967:144) and Peskin and Dwyer (Peskin and Dwyer 1964) suggested that the large spacing of the particles in a dilute suspension would permit the fluid to retain its original profile. However, both also suggested that the particles could introduce additional viscous dissipation into the fluid. Peskin and Dwyer further stated that particles occupying only a small volume could produce a thickened viscous sublayer and decrease the shear stress at the wall.

This investigation was the first reporting a thickened viscous sublayer in a gas-solids suspension. However, other

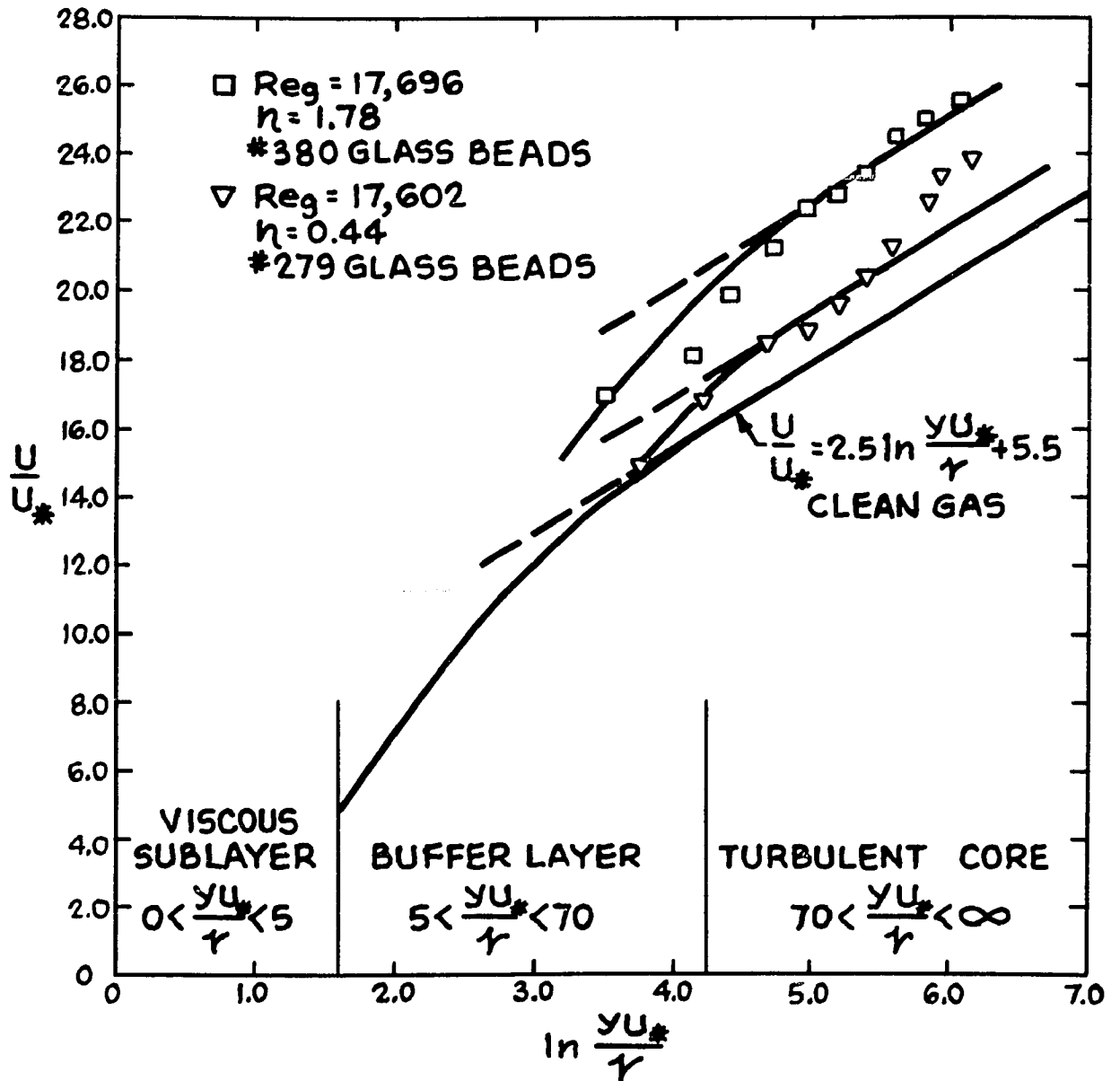


Figure 45
 Suspension Air Velocity Profiles and
 the Universal Velocity Profile

investigators working with different media have reported the same phenomena. Eissenberg and Bogue (Lumley 1964) reported a thickened sublayer for aqueous drag reducing suspensions of flocculated thorium. Rudd (Savins and Virk 1971:21-26) reported a thickened sublayer for drag reducing solutions of 0.01% Separan in water. The similarity of the results for the two early investigations and this one strongly indicate that drag reduction is the result of an interaction between the additive and the turbulent fluid in the near wall region.

SUSPENSION AIR TURBULENCE PROFILES

Turbulence intensity and turbulent spectral profiles were measured simultaneously with the suspension air velocity profiles using the fiber-film probe. The profiles all indicated that the presence of particles had the effect of increasing the air turbulence above clean air values.

Turbulence Intensity Profiles

Figures 46 and 47 show the results of two relative turbulence intensity profiles taken near the maximum drag reducing conditions for the #380 and #279 glass beads. Both profiles revealed higher turbulence intensities at all traverse locations compared to the clean air values. The increase was small until the wall was approached where the increase was dramatic. For example, the percent relative turbulence intensity at the traverse locations nearest the wall was 7.5% for clean air at a Reynolds number of about 18,000. The percent relative turbulence at this location for the suspensions of Figures 46 and 47 was 10.2% and 9.75% respectively.

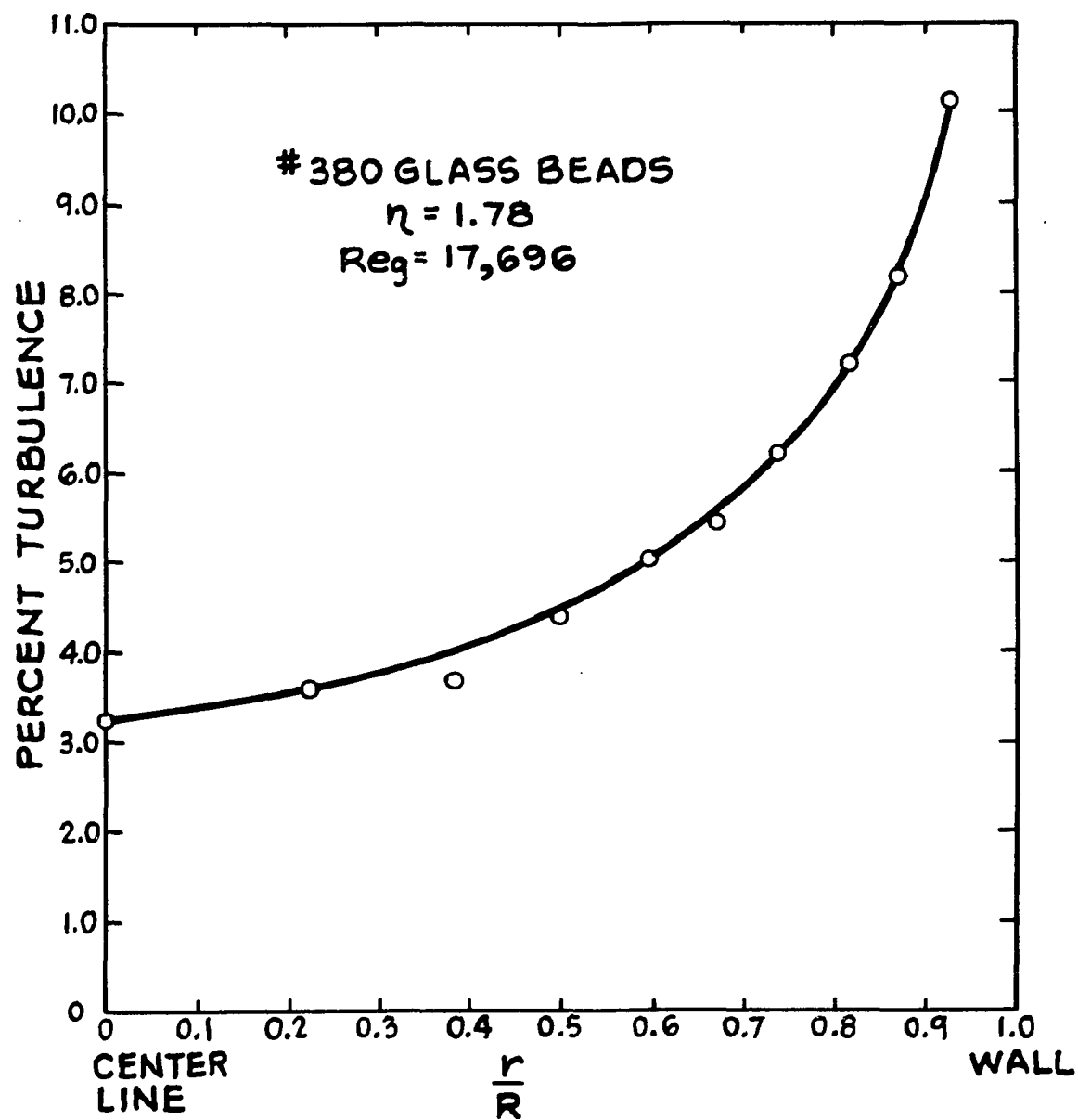


Figure 46
Suspension Air Turbulence Intensity Profile--
#380 Glass Beads

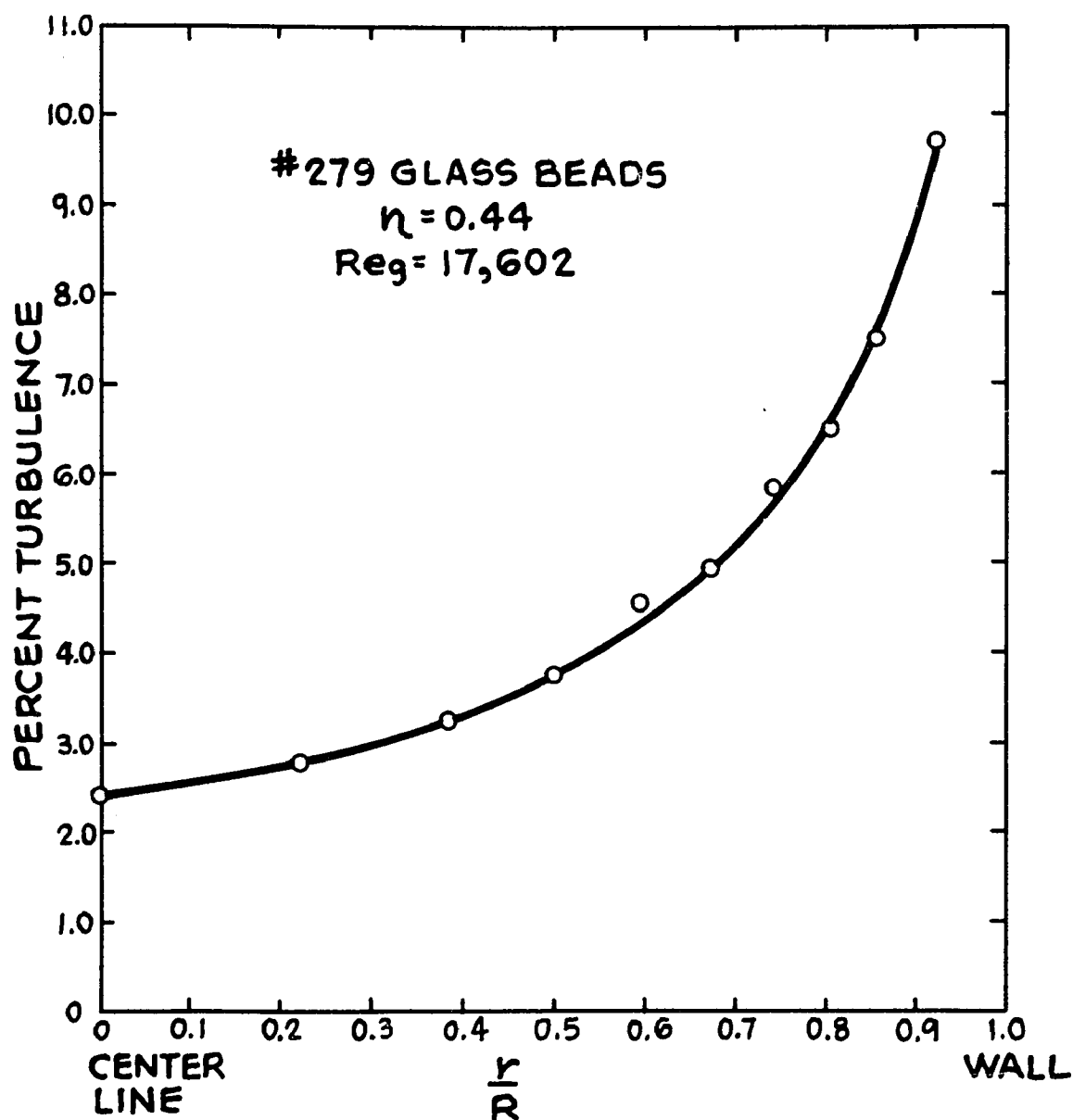


Figure 47
Suspension Air Turbulence Intensity
Profile--#279 Glass Beads

The result was in direct conflict with theories of drag reduction which predict an overall suppression of turbulence by the particles. Because of the conflict, the measurements were repeated for both clean air and suspension flows several times with the same results. The possible increase in measured turbulence caused by probe vibration was found to be negligible from the results of measurements made with a probe isolated from the flow but subject to the mechanical vibrations of the pump.

Only one other investigator (Rossetti 1969) has directly measured turbulence intensities in a gas-solids suspension using anemometry. Rossetti used a thermistor probe to record turbulence intensities at the centerline of a vertical section of his closed loop system. His measurements indicated that the turbulence intensities increased with loading ratio and drag reducing ability of the suspended solids. The results of this study were in general agreement with the results of the Rossetti work. Boothroyd (Boothroyd 1967) attempted to measure eddy diffusivity in vertical flowing and drag reducing gas-solids suspensions using gas dispersion techniques and was unable to obtain consistent results. Peskin performed two studies on a non-drag reducing suspension in a horizontal square duct and found in both cases that turbulence intensities decreased. The first study (Peskin and Rin 1964) used a helium gas tracer technique and the second study (Peskin and Wallace 1966) used a glow discharge probe. However, it has been reliably confirmed by several investigators using hot-film anemometry (Rodriguez, Patterson, and Zakin 1969) and laser Dopplerometry (Savins and Virk 1971: 21-26) that the effect of adding small quantities of drag

reducing polymers to water is to increase the turbulence intensities.

Figure 48 shows the results of Figure 46 normalized with friction velocity and plotted against non-dimensional distance to the wall. The envelope of the clean air data has been included for comparison. Figure 48 enforced the conclusions that drag reduction in a gas-solids suspension could be characterized by a thickening of the viscous sublayer. The known sharp increase in turbulence intensities in the buffer layer and outer edge of the viscous sublayer had been shifted away from the wall. Rudd, for one, also reported the same outward shift in the peak values of turbulence intensities. Therefore, it can be concluded, as pointed out by Davies (Davies 1972:293) that there is no general turbulence suppression in a drag reducing fluid.

Turbulent Spectral Profiles

Figure 49 shows the results of spectral measurements taken at the conditions of Figure 47 for the centerline and near wall positions. The clean air measurements have been repeated here for comparison. The spectra reflected the increased turbulence intensities measured in suspension flow with the appearance of higher spectral power densities at low wavenumbers. The questionable resolution of the probe and analyzer was apparent by the coincidence of the spectral power densities for both suspension and clean gas flows at high wavenumbers.

The two reports by Peskin (Peskin and Rin 1964; Peskin and Wallace 1966) on the turbulence properties of gas-solids suspensions agreed, in one sense, with the results reported here. The energy density of the gas at low wavenumbers was

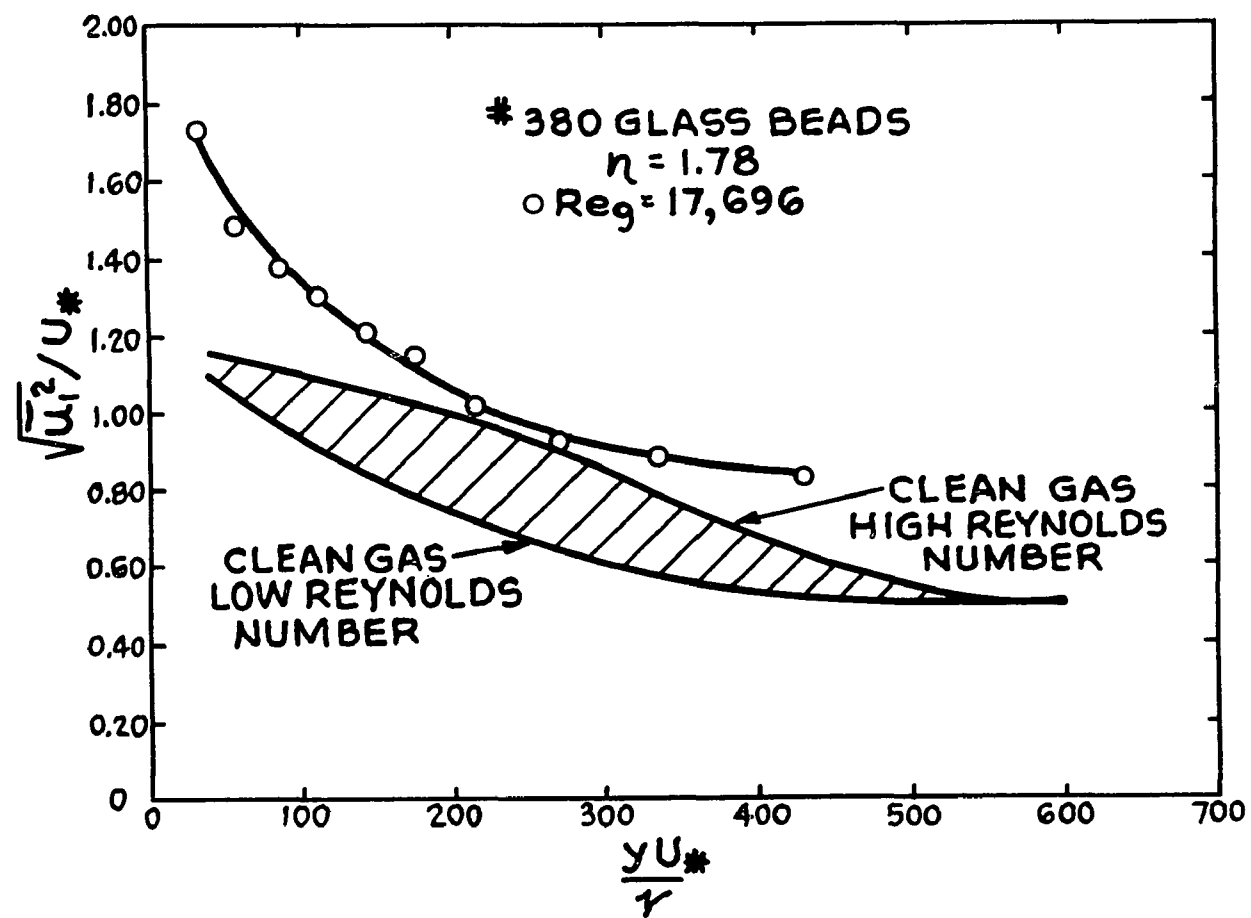


Figure 48 Suspension Air Turbulence Intensities Normalized with Friction Velocity

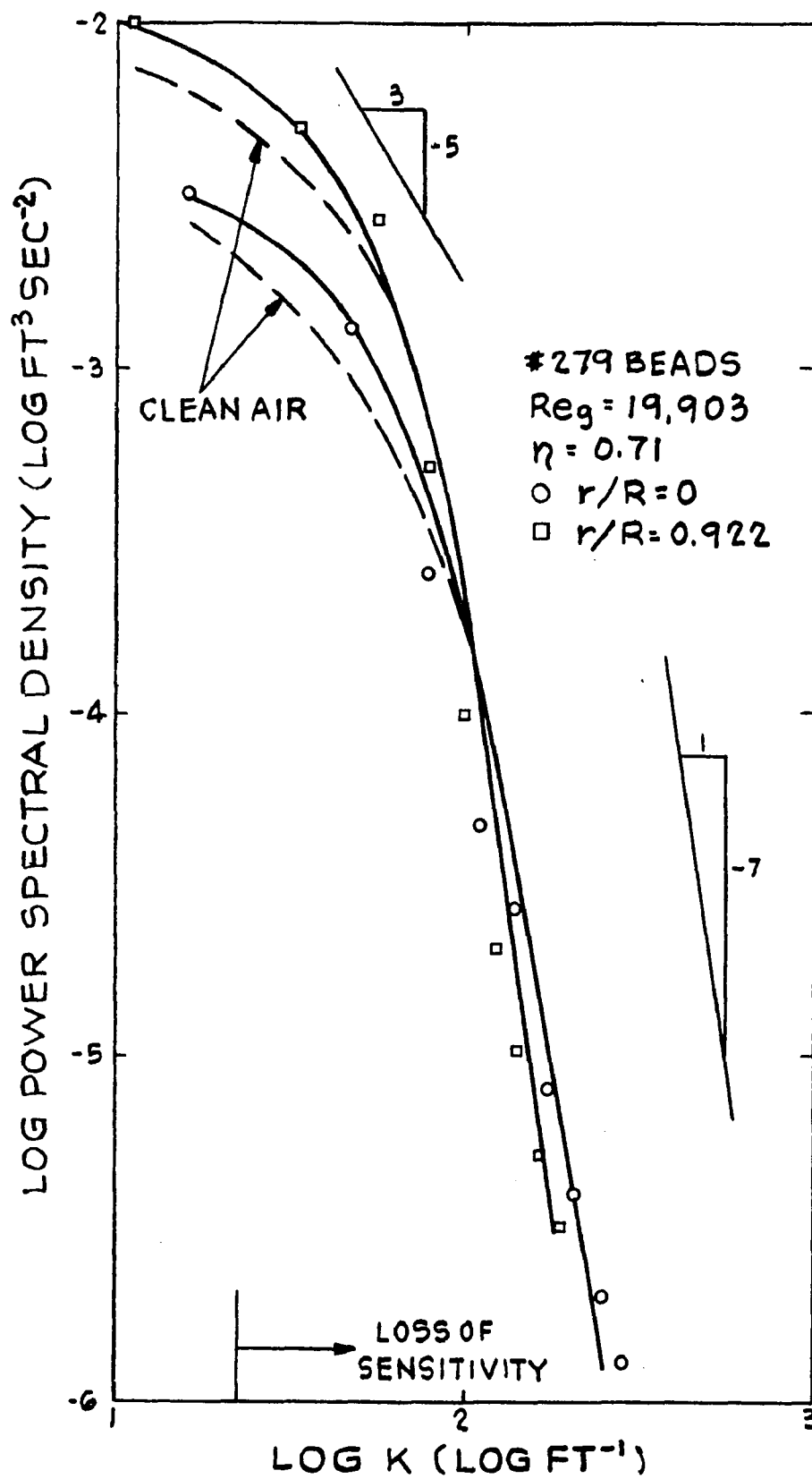


Figure 49

Suspension Air Spectrum Analyses

found to increase and to decrease at high wavenumbers. However, the flow conditions were not drag reducing. No previous investigation has attempted to measure spectra in a drag reducing gas-solids suspension.

Several investigators have attempted to measure turbulent spectra in drag reducing liquid polymer solutions. Unfortunately the results not only conflicted but have been questioned by the discovery that spectra obtained in viscoelastic polymer solutions are functions of probe geometry (Little 1969). A further complication has been provided by the knowledge that the viscous and elastic properties, if any, of the solvent were often drastically changed by the solute producing drastic changes in the spectrum. The most reliable hot-film anemometer measurements were made by Rodriguez, Zakin, and Patterson (Rodriguez, Zakin, and Patterson 1969) who reported that energy spectra in elastic drag reducing polymer solutions shifted to higher frequencies in comparison to purely viscous fluids of the same viscosity. This result was in agreement with the results of this investigation if the particles were considered as part of an elastic energy storage mechanism.

If, as seems apparent, the gas-solids drag reduction can be characterized by a thickened viscous sublayer, the energy spectrum at a particular radial location in the turbulent core would be expected to reflect an increase in turbulent production and a decrease in viscous dissipation. The spectra in the turbulent core would be expected to show a larger k^{-1} production region and a smaller k^{-7} dissipative region. In contrast the spectrum in the expanded viscous sublayer and buffer layer would be expected to show the opposite tendency.

PARTICLE VELOCITY PROFILES

Particle velocity profiles were measured for the three largest glass bead sizes. Two profiles at different loading ratios were taken at approximately the same gas Reynolds numbers for the #660 glass beads. One profile each was taken for the #380 and #279 glass beads. Although the streak patterns for the #660 glass beads were easily recognized, the patterns became difficult to discern with the smaller particle sizes. Because the streaks caused by the movement of the #279 particles were barely visible, velocity profiles for the smaller #980 glass beads were not measured.

The results shown in Figure 50 for the specific case of the #660 beads at a loading ratio of 2.23 and a gas Reynolds number of 16,400 were nevertheless, typical of all particle velocity profiles, regardless of particle size, loading ratio, or gas Reynolds numbers. The particle velocity profiles were, within the limits of the camera resolution and measurement accuracy, the same as the air velocity profiles with the exception of a finite slip velocity at the wall. (The last point of the photographic traverse was measured at the wall and always indicated a finite velocity.) The smaller #980 glass beads would certainly have shown the same tendency toward following the mean value of the local gas velocity. The finite depth of field of the camera prevented utilization of the photographs to determine accurate values of the slip velocity in the viscous sublayer. However, the results confirmed the assumption that the loading ratio could be used to approximate the ratio of the dispersed solids density to the air density in the vertical test section.

The results were not unexpected because the terminal

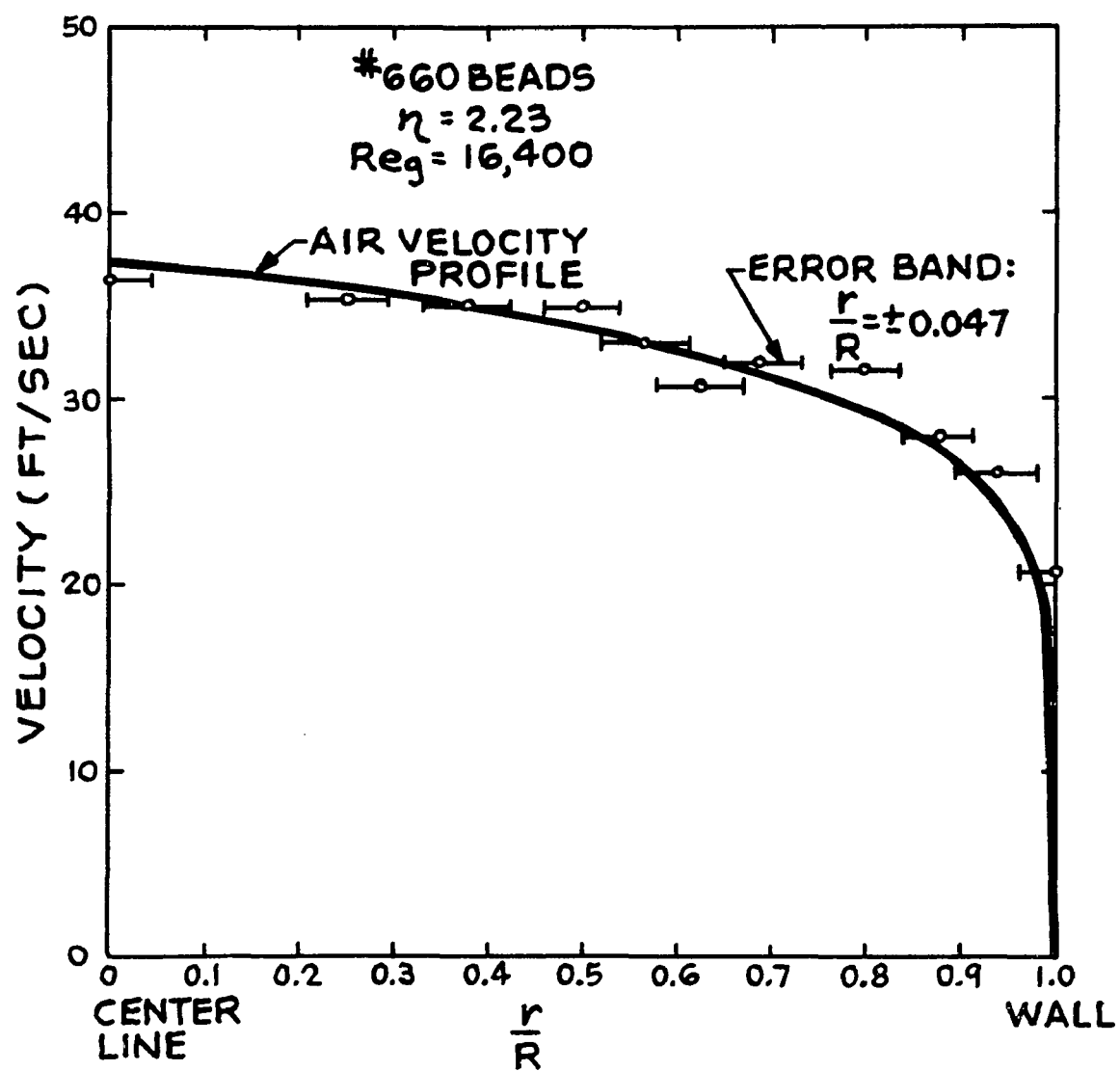


Figure 50
Particle Velocity Profile

settling velocities of the beads were only 0.744, 0.329, and 0.119 feet per second for the #660, #380, and #279 glass beads respectively. Apparently, the particle separation in the vertical test section, even at the highest loading ratios of the study, was large enough to minimize the number of collisions among particles and to minimize the slip velocity between the gas and the particles. Although there is some question of the applicability of a continuum theory to the widely dispersed particles of a dilute suspension (Soo 1967:141; Boothroyd 1971:64-65) the theory of Soo and Tung (Soo and Tung 1971) suggests that the particle velocity profile should approach that of the gas for particles with small relaxation times and negligible electrical charge.

The literature on direct photographic measurement of particle velocities in the turbulent flow of a gas-solids suspension in a vertical tube includes the work of Doig and Roper (Doig and Roper 1967), Reddy, Van Wijk, and Pei (Reddy, Van Wijk, and Pei 1967), Reddy and Pei (Reddy and Pei 1969), and Kramer and Depew (Kramer and Depew 1972a). The results reported by Doig and Roper do not provide a good comparison to this study because the particle diameters, 304μ and 756μ , were much larger than the ones considered here. The second Reddy report included all the experimental results of the first one. However, the first report contained the information that the axial particle velocities in a dilute suspension obtained by three-dimensional stereophotogrammetry and those obtained by two-dimensional photographs, the system used in this study, were the same. This means that most particles in a dilute gas-solids suspension must travel parallel to the tube walls. Consequently errors introduced into this study by ignoring the possibility of particles

entering and leaving the camera focal plane during a flash sequence were probably negligible.

Reddy and Pei considered 100, 150, 200, and 270 μ glass beads in suspension at loading ratios of 0.02, 0.06, 0.10, 0.15, 0.20, 0.40, and 0.60 and gas Reynolds numbers of 55,000; 78,000; and 100,000. They reported that the particle axial velocity profile in the turbulent core was similar to that of the gas and could be described by a power law relationship. The equation:

$$\frac{V_{\text{pcs}}}{V_t} = 1.0 + 1.59\eta^{2/3} \quad (45)$$

was proposed to account for the variation of the centerline slip velocity with particle diameter and loading ratio. If this equation is extrapolated to the experimental range of this investigation, the centerline slip velocity (defined as the difference between the gas velocity and the particle velocity) corresponding to the conditions of Figure 50 is only 2.76 feet per second, well within the limits of experimental uncertainty. Therefore, this study was in substantial agreement with the results of Reddy and Pei.

Kramer and Depew reported the velocity profiles of 62 μ and 200 μ spherical glass beads in 0.5, 0.75, and 1.0 inch diameter tubes containing suspensions at gas Reynolds numbers from 5,760 to 50,000 and loading ratios from 0 to 5.0. The range of parameters for the 62 μ glass beads corresponded closely to the range of parameters investigated during this study for the #660 glass beads. Kramer and Depew found that the slip velocity was quite small for the 62 μ glass beads and did not vary significantly with loading ratio. The slip velocity did vary with the radial coordinate

and decreased, even to negative values, toward the wall. The results of this study were in good agreement with those of Kramer and Depew. Therefore, this study confirmed the contention of Kramer and Depew that their continuum approach (Kramer and Depew 1972b) to the calculation of suspension velocity profiles which predicted significant bulk slip velocities requires modification to provide better agreement of calculated results with experimental results for small particle sizes.

The studies by Reddy and Pei and Kramer and Depew included measurements of particle concentration profiles. Reddy and Pei counted the number of particles directly from the stereophotographs and compared the results to the number calculated from the loading ratio and particle velocity. The agreement was within 10 percent. The distribution calculated by either method was uniform.

Kramer and Depew used a photomultiplier tube to count the particles passing through an illuminated control volume. They found that the suspension density profile was approximately uniform at loading ratios between 0 and 3.0 for the 62 μ glass beads. There was a weak maximum at the centerline which became more pronounced as the loading ratio increased to 5.0. However, their results suggest that the concentration profile at the dilute loadings of this study were uniform.

The number of particle streaks on one photograph from this study was counted in order to determine if the correct number of particles was being recorded by the camera. The count was close to the value expected from the value of the loading ratio and an assumed uniform particle density. The count provided further confirmation that the particles, on

average, travelled parallel to the tube walls. The experimental plan did not call for measurement of particle density so that the additional time and a more sophisticated film processing scheme was not provided. Although it was not essential to measure density profiles in the investigation, the photographs, if processed more carefully, could have been used to further corroborate the Reddy and Pei and Kramer and Depew density measurements.

SUSPENSION NUSSELT NUMBERS

The suspension heat transfer measurements were used to calculate Nusselt numbers for two gases, air and argon, carrying suspended glass beads or aluminum powder. The circulation of suspensions required revision of the clean gas formulae for heat input in the vertical test section and heat loss in the horizontal test section. The mass flow rate now included the contribution of the suspended solids:

$$W_s = W_g (1 + \eta) \quad (46)$$

The specific heat of the suspension was calculated from a weighted average of the separate gas and solids specific heats:

$$C_{ps} = C_{pg} \frac{1 + \delta \eta}{1 + \eta}$$

where (47)

$$\delta = \frac{C_{pp}}{C_{pg}}$$

The final calculations and correlations of Nusselt numbers were based on the gas properties (density, thermal

conductivity, specific heat, and viscosity) in order to provide a direct comparison with the clean gas results. For the loading ratios of this study, the assumption that the effective thermal conductivity of the suspension was the same as the gas thermal conductivity introduced negligible error (Pfeffer, Rossetti, Lieblein 1966:6).

Vertical Test Section

The results of the vertical test section heat transfer measurements are shown in Figure 51 for suspensions of the #279 (21.6 μ) glass beads in air and in Figure 52 for suspensions of the #380 (36.0 μ) glass beads in air and argon and suspensions of H-322 (30.0 μ) aluminum powder in argon. The graphs were plotted as the ratio of the suspension Nusselt number to the clean gas Nusselt number versus loading ratio at corresponding gas Reynolds numbers. Nusselt number reduction was indicated by values of the Nusselt number ratio below unity. Figure 52 shows the envelope of the actual data. The upper limit generally represented the argon-glass results. The lower limit generally represented the air-glass and argon-aluminum results.

The glass-air suspension measurements indicated that the #279 and #380 glass beads were much more effective in reducing Nusselt numbers than in reducing friction factors. Reductions of up to 44% and 52% were achieved in the vertical test section for the #279 and #380 glass beads respectively. Like the friction factor results, there was a minimum loading ratio for the onset of Nusselt number reduction. Also like the friction factor results, the onset of Nusselt number reduction was further delayed with increased particle size. Neither of the two glass bead suspensions showed any Reynolds

VERTICAL FLOW
21.6 μ GLASS BEADS
IN AIR

$$11,000 < Re_g < 21,000$$

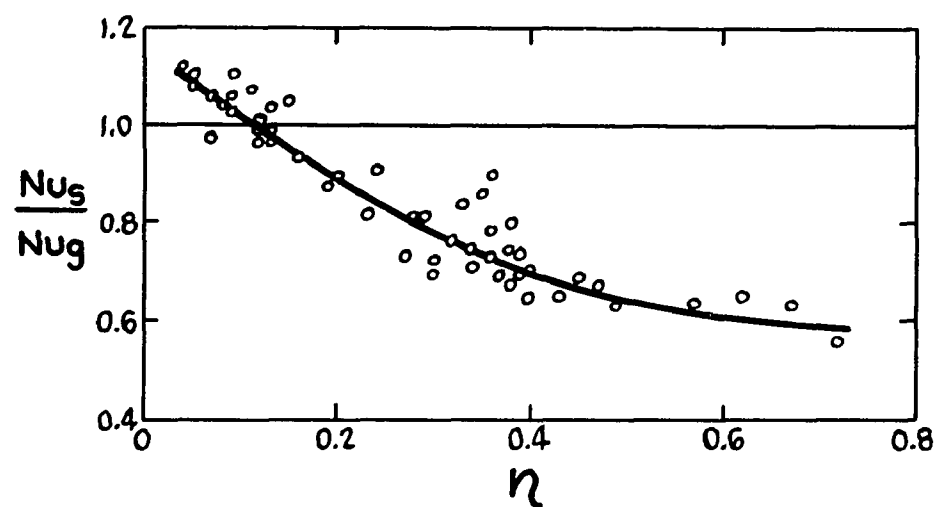


Figure 51 Vertical Nusselt Number Ratio Versus
Loading Ratio--#279 Glass Beads

VERTICAL FLOW
 36 μ GLASS BEADS
 30 μ ALUMINUM POWDER
 11,000 < Re_g < 21,000

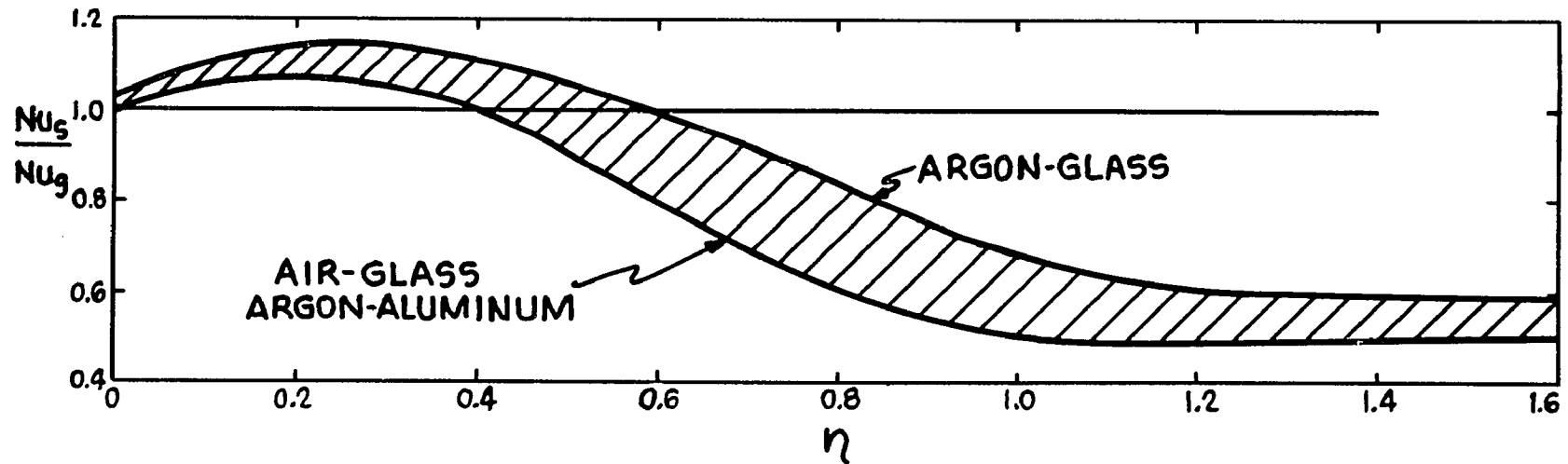


Figure 52 Vertical Nusselt Number Ratio Versus Loading Ratio--#380 Glass Beads and H-322 Aluminum Powder

number effect on the onset of Nusselt number reduction or on the amount of Nusselt number reduction.

The suspensions of #279 glass beads showed a monotonic decrease in Nusselt number ratio after the onset of Nusselt number reduction. Before the onset of Nusselt number reduction at a loading ratio of 0.12, a moderate increase in Nusselt number was noted. The increase was attributed to an initial widely dispersed deposit on the test section wall. The deposit was apparently insufficient to cover large heat transfer areas but was sufficient to disturb the viscous sublayer, producing additional turbulence and heat transfer.

The suspensions of #380 glass bead in air showed a tendency toward a flat minimum at a loading ratio of 1.2. Before the onset of Nusselt number reduction at a loading ratio of 0.4, a slight increase in Nusselt number was noted. The increase was also attributed to a widely dispersed deposit on the vertical test section wall. That the increase was smaller for the #380 glass beads than for the #279 glass beads was consistent with the relative ability of the two bead sizes to plate out. For both bead sizes, the deposit, rather than accumulating further, was probably worn away by heavier loadings and did not affect the vertical test section results past the onset of Nusselt number reduction.

The measurements of Nusselt numbers for suspensions of the #380 glass beads in argon were performed in order to determine the effect of different gas properties on the Nusselt number reduction. The argon appeared to delay the onset of Nusselt number reduction to a loading ratio of 0.6 compared to 0.4 for air. However, the maximum amount of Nusselt number reduction was approximately the same for both gases. The general shape of the Nusselt number ratio versus

loading ratio curves was also the same for both gases. These results were not surprising since the Prandtl numbers of air and argon were similar in the working temperature range.

The heat transfer measurements for suspensions of aluminum powder in argon indicated that particle thermal properties had no effect on Nusselt number reduction in the vertical test section. The aluminum powder at a nominal diameter of 30μ gave similar results as the #380 glass beads at a nominal diameter of 36μ . The similarity in behavior of the two different particles in the vertical test section suggested that the Nusselt number reduction was a coincidental result of the postulated drag reduction mechanism; that is, a thickening of the viscous sublayer.

No measurements were made for the other three glass bead designations, #980, #981, and #660. The #980 beads were known to be extremely difficult to circulate as their small size permitted rapid deposition and plating on the loop walls. The #981 beads were essentially identical in size to the #279 particles and could not provide any additional information. The #660 glass beads were known to degrade after prolonged passage through the pump and could not give reliable data after the long warmup periods required for heat transfer measurements. The aluminum powder was selected for tests because it had different thermal properties from the glass beads and might give additional information on the heat transfer mechanism. Argon was chosen as the conveying medium for circulation of aluminum powder; because, it would eliminate any possible explosion hazard in the closed loop system caused by the buildup and release of static electric charges.

Horizontal Test Section

The results of the horizontal test section heat transfer measurements are shown in Figure 53 for suspensions of the #279 glass beads in air and in Figure 54 for suspensions of the #380 glass beads in air and argon and suspensions of H-322 aluminum powder in argon.

Significant Nusselt number reduction, up to 38% and 44% for the #279 and #380 glass beads in air respectively, was also achieved for the horizontal test section. However, the horizontal test section Nusselt number results were not as consistent with the friction factor results as those in the vertical test section. Gravity sedimentation and thermophoresis causing deposition on the cool walls of the horizontal heat transfer test section apparently complicated the results.

The #279 glass beads showed very similar results in both horizontal and vertical test sections with the exception of a detectable Reynolds number effect in the horizontal test section. The amount of Nusselt number reduction progressively decreased with decreasing Reynolds number. The onset of Nusselt number reduction was progressively delayed, from loading ratios of 0.07 to 0.16 with decreasing Reynolds numbers until the lowest Reynolds numbers, about 12,000, was reached. At Reynolds numbers of about 12,000, Nusselt number reduction was not generally noted. The observed loss of entrainment at these Reynolds numbers presumably could explain the anomalous results as the presence of large quantities of solids in direct contact with the tube would drastically change the heat transfer mechanism. With the exception of the low Reynolds number results, the trends of

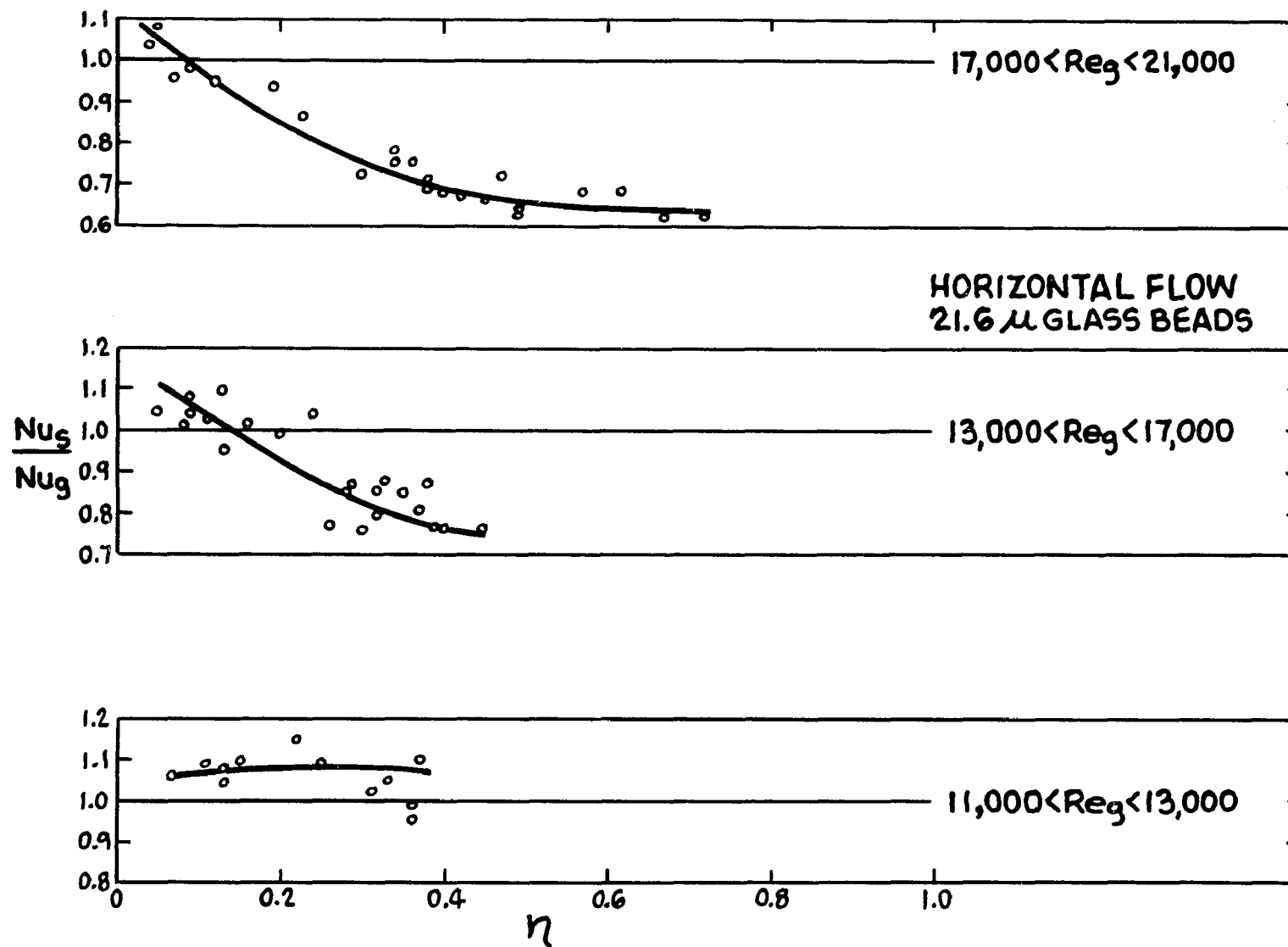


Figure 53 Horizontal Nusselt Number Ratio Versus Loading
Ratio--#279 Glass Beads

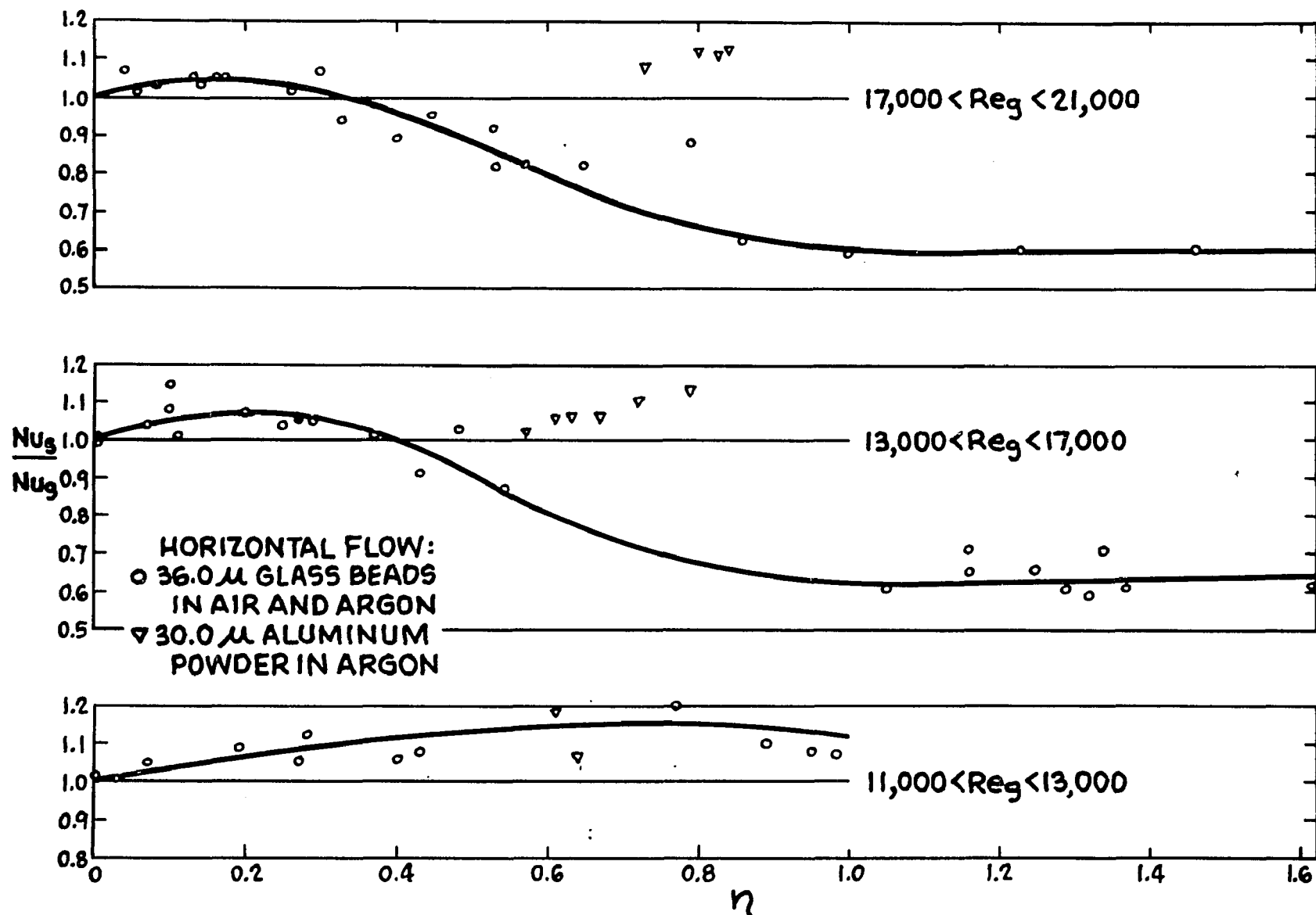


Figure 54 Horizontal Nusselt Number Ratio Versus Loading Ratio--#380 Glass Beads and H-322 Aluminum Powder

the heat transfer measurements for the #279 glass beads were reasonably consistent with the trends of the pressure drop measurements. However, it could not be concluded that the controlling mechanism in the horizontal test section was the same for both cases. Thickening of the sublayer was probably more important for the pressure drop measurements; and, particle deposition was probably more important for the heat transfer measurements.

The results for suspensions of #380 glass beads in air were less predictable and systematic than the results for the #279 glass beads. Whereas, friction factor increases were noted at all test conditions for the #380 glass beads, Nusselt number increases were found only for loading ratios less than 0.4 or for Reynolds numbers of about 12,000. In general, substantial Nusselt number decreases were observed. The friction factor and heat transfer results for the #380 glass beads tended to confirm the conjecture that particle deposition was more important for the Nusselt number reduction in the horizontal test section than any uniform sublayer changes. The onset and amount of Nusselt number reduction did not follow the consistent pattern observed with tests of #279 glass bead suspensions. It was apparent that the Reynolds analogy which predicts corresponding effects on heat transfer and momentum transfer was not valid for horizontal flow. The particles did not just distort the structure of the turbulence, as they did in vertical flow, but actually became an active part of the heat transfer mechanism.

The data obtained with suspensions of #380 glass beads in argon were in agreement with the air suspension data. However, the aluminum-argon suspension data always indicated

Nusselt number increases regardless of loading ratio or Reynolds number. These results were probably caused by the deposition of the powder on the cool wall of the horizontal test section. The unusual nature of the aluminum deposits probably disturbed the flow pattern and produced additional turbulence. Reductions in local heat transfer rates directly at the deposit locations were probably quite small because of the high thermal conductivity of the aluminum. Therefore, it was most likely that the additional turbulence aiding heat transfer more than offset the local fouling factors hindering heat transfer.

Comparison to Results of Other Investigators

Many investigators have reported Nusselt number reduction at low values of loading ratio for a wide variety of flow conditions. Several investigations (Tien and Quan 1962; Depew and Farbar 1963; Farbar and Depew 1963; Depew and Cramer 1970) were conducted at almost exactly the same conditions as reported here. The agreement of the trends and, in some instances, the numerical results with the trends and results of this investigation was exceptional.

Tien and Quan measured heat transfer coefficients of suspensions of 28 μ glass beads in air flowing in a heated, vertical, 0.710 inch I.D., stainless steel tube, at gas Reynolds numbers of 15,000 and 30,000. They found Nusselt number reductions of as much as 38% with the minimum Nusselt number ratio occurring at a loading ratio of 1.0. Their results compared favorably with the amount and location of the minimum Nusselt number ratio found here for the #380 and #279 glass beads.

Depew and Farbar measured heat transfer coefficients of suspensions of 28 μ glass beads in air flowing in a heated,

vertical, 0.710 inch I.D. stainless steel tube at gas Reynolds numbers of 13,500 and 27,400. They also reported minimum Nusselt number ratios at loading ratios around 1.0. The onset of Nusselt number reduction was delayed until loading ratios of 0.5, in excellent agreement with the results of this study. In another paper (Farbar and Depew 1963) Depew and Farbar reported a slight increase in Nusselt number ratio at loading ratios below 0.5, also in agreement with the results of this study.

Depew and Cramer measured heat transfer coefficients of suspensions of 28 μ glass beads in air flowing in a heated, horizontal, 0.710 inch I.D. stainless steel tube at gas Reynolds numbers of 10,000; 15,000; and 30,000. Nusselt numbers were calculated for both the top and bottom of the tube with significant difference being found. They found that the Nusselt number for the suspension was reduced below that of air alone at loading ratios below 2.0 for both the top and bottom of the tube. At loading ratios above 2.0 the Nusselt numbers remained low for the top of the tube and increased for the bottom of the tube. Depew and Cramer also measured pressure drops in the horizontal tube for the 28 μ glass beads and found a slight increase in pressure drop over clean air values with increasing loading ratios up to 4.5. Their results like the results of this study indicated that it was possible to have increased pressure drops coincident with reduced heat transfer coefficients. Their results also suggested that density stratification and deposition were more important, in a horizontal flow, than particle sublayer interaction.

Chapter 6

THEORETICAL IMPLICATIONS AND DISCUSSIONS

The results of the profile measurements lent strong support to the belief that gas-solids drag reduction can be characterized by changes in the viscous sublayer. Only one previous theory, that of Jotaki and Tomita (Jotaki and Tomita 1971) has attempted to define a relationship between the sublayer thickness and the flow parameters of a dusty gas. However, their theory did not develop a relationship between changes in sublayer thickness and the amount of drag reduction. This relationship was developed during this study by extending and modifying the theory of Jotaki and Tomita to account for factors which were omitted in the original analysis. In the new form, the theory provided relationships between loading ratio, gas Reynolds number, and friction factor ratio which were in good agreement with the results of Chapter 5.

This chapter begins with a discussion of the validity of other theories of drag reduction, as outlined in Chapter 2, in light of the experimental results. A full and detailed description of the theory of Jotaki and Tomita follows the discussion. The chapter concludes with the description and results of the modified theory.

EVALUATION OF THEORIES OF DRAG REDUCTION

Decrease of Gas Viscosity

The possible reduction of the viscosity of the suspending gas by the presence of particles (Sproull 1961) has already been discounted and disproved quite thoroughly by the work of Saffman (Saffman 1962b) and Chand (Chand 1970). The particles were too large to engage in the random motions of kinetic molecular theory and were too far apart to affect the mean free path of the gas molecules. The possible reduction of viscosity by the presence of particles conflicted with the Einstein equation which predicted an increase in effective gas viscosity on the basis of increased resistance to shearing motions (Batchelor 1967:253).

Electrostatic Effects

The electrification of the suspended particles and the pipe wall has been used by several authors (Richardson and McLeman 1960; Owen 1969) to explain the appearance of drag reduction. The results of earlier studies (Richardson and McLeman 1960; Boothroyd 1966) and this one refute this explanation. The work of Richardson and McLeman showed that the presence of electrostatic charges was important and apparent only if the measured pressure drop increased with time of circulation. In the one case where Richardson and McLeman observed drag reduction, the pressure drop did not vary with time of circulation. Their own results proved that the explanation used for the observed increases in pressure drop with the addition of particles, gradual electrification of the system, could not be the cause for the one case of drag reduction. The measured pressure drop did not gradually decrease with time.

Richardson and McLeman stated that the electrification in the drag reducing case produced a decrease in particle concentration near the wall and a subsequent decrease in pressure drop. Richardson and McLeman offered no reason why a decrease in particle concentration will, of itself, produce a decrease in friction factor. In contrast, Owen states as a premise for his theory of drag reduction that particle electrification produced an increase in particle concentration near the wall. Owen believed that the fluid turbulence expended itself to avoid particle deposition by working on the particles as they drifted toward the wall. However, Owen offered no reason why a fluid, of itself, should work to avoid particle deposition.

This study and that of Boothroyd did not find that pressure drops varied with time of circulation. Boothroyd's explanation that fine particles provide a thin insulating coating on the pipe wall seems quite reasonable. The resultant inhibition of charge transfer at the pipe wall permits uniform density distributions (in dilute phase vertical flow) and constant, replicable pressure drop measurements. Any time dependence on pressure drop would certainly have been seen during this study as the suspension was continuously circulated through a closed loop system. That no time dependence was observed here or in any other reported case of drag reduction is considered conclusive proof that electrostatic charging is not the source of gas-solids drag reduction.

General Turbulence Suppression

The presence of particles did have an effect upon the turbulent structure of the fluid. The turbulence intensity

measurements showed that the effect was most important near the wall and least important in the center of the pipe. In all cases, the presence of particles increased the turbulence intensity of the fluid. There was no general suppression of turbulence. Therefore, the suggestion that drag reduction is the result of a general decrease in the turbulence level is incorrect (Schluderberg, Whitelaw, and Carlson 1961; Tien and Quan 1962; Soo 1967:144).

Thickening of the Sublayer

The velocity and turbulence intensity profiles reported in Chapter 5 strongly supported the idea that drag reduction can be characterized by changes, that is thickening, of the viscous sublayer. The several theories (Saffman 1962b; Michael 1964; Liu 1965; Lumley 1964; Boothroyd 1967) stating that particles are responsive to some eddy sizes and unresponsive to others are generally consistent with theories (Peskin and Dwyer 1964; Lumley 1964; Jotaki and Tomita 1971) predicting thickened sublayers. Therefore, all the theories of particle turbulence interaction can be considered as part of a unified treatment on the effect of particles on the shear flow structure in a pipe. (A general discussion of the interaction of length and time scales with turbulence in drag reducing flows is given in Appendix C). For the purposes of predicting gas-solids drag reduction, the treatment can be localized in the sublayer region. It will be shown later that predicted increases in the sublayer thickness can be used to predict decreases in the friction factor without considering the outer flow.

In order to calculate the effect of particles on the structure of the sublayer region, a good physical and mathematical description of the sublayer region is required. Recent studies (Bakewell and Lumley 1967; Kline, Reynolds,

Schraub, and Runstadler 1967; Corino and Brodkey 1969) have shown that the sublayer is not the quiescent laminar region assumed in most textbooks.

The structure of the sublayer can be described with reference to the energy budget in the region (Hinze 1959: 529). The dissipation of turbulent energy in this region is balanced by the viscous transfer of kinetic energy into the region. The size of the sublayer is determined by the size of the largest disturbance that can be maintained in equilibrium there. Smaller disturbances will be dissipated and destroyed by the action of viscosity. Larger disturbances will grow and become part of the intense energy production in the buffer zone outside the sublayer. The largest equilibrium disturbance has been postulated (Townsend 1956:210) and found (Bakewell and Lumley 1967) to have an organized structure in the form of randomly distributed, streamwise oriented, counterrotating pairs of standing eddies attached to the wall. The Townsend-Bakewell eddies appear to play an important role in balancing the transfer of turbulent energy into the sublayer with the viscous dissipation near the wall. The Townsend-Bakewell eddies feed from the mean flow and lose energy by working against smaller dissipative eddies which themselves are eventually destroyed by viscous dissipation.

In a drag reducing suspension flow, the particle relaxation time will be too large to respond to the smaller energy producing eddies near the wall and will act as an additional dissipative mechanism. Particles can, therefore, serve to increase the size of the largest possible equilibrium disturbance in the sublayer and increase the size of the sublayer itself. Farther from the wall, the size and time

scales of the energy producing eddies increase. The particles may respond to the eddies in this region. It is possible and even likely that the same size particles which provide a net increase of dissipation in the sublayer will also provide a net increase of turbulent production in the turbulent core.

The measurements of turbulence intensity in suspension flow showed that the presence of particles increased the turbulence in the core and also thickened the sublayer. The location of the maximum (with radial position) turbulence intensity shifted away from the wall. The increase in turbulent production was highest at a given loading ratio for the smallest particle sizes. This was consistent with the increased response of smaller particles to energy producing eddies as compared with larger particles.

The picture of particles having a dual role in both dissipation and production of turbulent energy is in agreement with the thoughts of Boothroyd, Saffman, and Liu. Those workers who felt that drag reduction was the result of general turbulence suppression did not recognize the large variation in the turbulent scale between the wall and the center of the pipe in shear flow. A fixed particle size cannot respond in the same way to the small scale turbulence near the wall and to the large scale turbulence far from the wall. However, the difficulty in determining the full response of particles to a widely varying turbulent structure can be minimized by considering the interaction in the sublayer alone. This was precisely what Jotaki and Tomita chose to do and was the most promising course to follow in the development of a quantitative theory.

THEORY OF JOTAKI AND TOMITA

The following derivation for the thickness of the viscous sublayer in terms of suspension flow parameters was based on the work of Jotaki and Tomita. However, greater detail is reported here than was reported in the original paper (Jotaki and Tomita 1971). The Navier-Stokes equations as modified by Saffman to include an additional Stokesian particle drag on the fluid were used by Jotaki and Tomita:

$$\rho \left(\frac{\partial u_i}{\partial t} + u_l \frac{\partial u_i}{\partial x_l} \right) = -g_c \frac{\partial p}{\partial x_i} + \rho \nu \frac{\partial^2 u_i}{\partial x_l \partial x_l} + KN(\nu_i - u_i) \quad (48)$$

$i, l = 1, 2, 3$

If the particles are small spheres:

$$K = 3\pi a \rho \nu \quad (49)$$

The continuity equation for the fluid was considered in its normal incompressible form:

$$\frac{\partial u_i}{\partial x_i} = 0 \quad (50)$$

Jotaki and Tomita assumed that the particles were so small that the local mean velocity of the gas and the particles were identical. At the same time, they assumed that the particles were still too large to respond at all to the turbulent fluctuations of the fluid.

Only that part of the mean flow distribution near the wall was considered. In turbulent flow, the viscosity distribution within the viscous sublayer was represented by the linear law:

$$\frac{U}{U_*} = \frac{U_* x_2}{\nu} \quad \text{for} \quad \frac{U_* x_2}{\nu} \leq R_\nu \quad (51)$$

Farther from the wall, the velocity distribution was represented by the logarithmic law:

$$\frac{U}{U_*} = \frac{1}{K} \ln \frac{U_* x_2}{\nu} + B \quad \text{for } \frac{U_* x_2}{\nu} \geq R_N \quad (52)$$

The value of the constants K (where K is a Von Karman type constant) and B were determined by assuming a smooth variation in velocity profile at the edge of the viscous sublayer:

$$\left. \frac{d(U/U_*)}{d(U_* x_2 / \nu)} \right|_{R_N} = 1$$

and

$$\left. \frac{d(U/U_*)}{d(U_* x_2 / \nu)} \right|_{R_N} = \frac{1}{K(U_* x_2 / \nu)} \bigg|_{R_N} = \frac{1}{K R_N} \quad \therefore K = \frac{1}{R_N} \quad (53)$$

Also:

$$\left. \frac{U}{U_*} \right|_{R_N} = R_N$$

and

$$\left. \frac{U}{U_*} \right|_{R_N} = R_N \ln R_N + B \quad \therefore B = R_N (1 - \ln R_N) \quad (54)$$

The combination of the two laws, linear and logarithmic, gave:

$$\frac{U}{U_*} = R_N \ln \frac{U_* x_2}{\nu} + R_N (1 - \ln R_N) \quad (55)$$

Equation 55 gives an excellent description of the velocity profile through the buffer zone and practically the entire sublayer (Jotaki and Tomita: personal communication 1971).

The local fluid velocity was represented by:

$$u_i = U_i + u_i' + u_i'' \quad (56)$$

The local mean velocity, having a component only in the longitudinal direction, parallel to the wall, was described by equation 55. The fluctuating portion of the local fluid velocity was broken into two parts. The first part was characterized by the velocity components of a Townsend-Bakewell attached eddy in the sublayer:

$$u_1' = -u_2' = A\alpha^2\chi_2\chi_3 e^{-\frac{1}{2}\alpha^2(\chi_2^2 + \chi_3^2)} \quad (57)$$

$$u_3' = -A(1 - \alpha^2\chi_2^2) e^{-\frac{1}{2}\alpha^2(\chi_2^2 + \chi_3^2)} \quad (58)$$

Townsend (Townsend 1956:208) postulated the forms of these velocity components from measured correlations in pipe flow. The exponential variation provides the proper random nature and decay with distance. In the sublayer, these components represented the largest eddy components of the fluctuating velocity. Figure 55 shows the form of the Townsend-Bakewell eddy described by equations 57 and 58. The other part of the fluctuating velocity, u_i'' , represented the smaller energy dissipative eddies in the sublayer and buffer zone.

Because the mean velocity profiles of the particles and the fluid were assumed identical, the difference between the two local velocities was given by:

$$v_i - u_i = -(u_i' + u_i'') \quad (59)$$

Assuming a steady mean flow, the Navier-Stokes equations became:

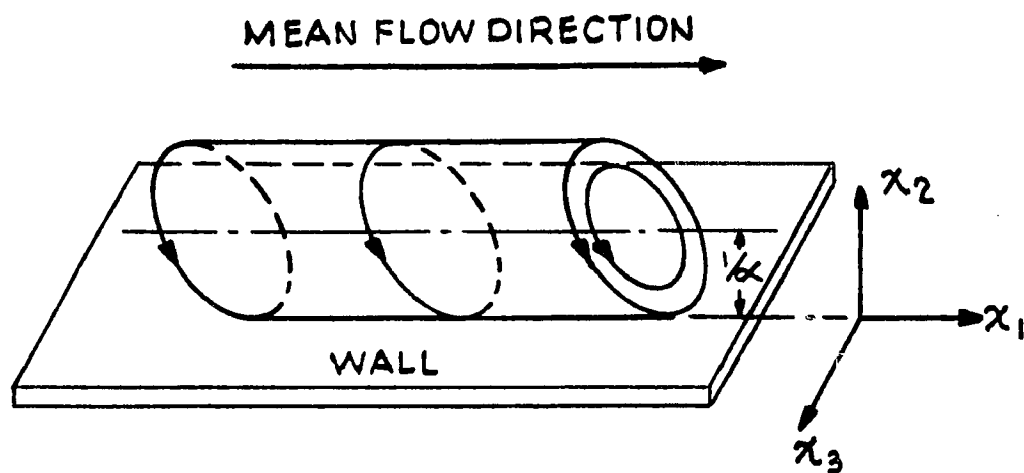


Figure 55
Townsend-Bakewell Eddy

$$\begin{aligned} & \frac{\partial}{\partial t}(u_i' + u_i'') + (U_x + u_x' + u_x'') \frac{\partial}{\partial x_x}(U_i + u_i' + u_i'') \\ &= -\frac{g_c}{\rho} \frac{\partial}{\partial x_i}(P + P' + P'') + \nu \frac{\partial^2}{\partial x_x \partial x_x}(U_i + u_i' + u_i'') - \frac{KN}{\rho}(u_i' + u_i'') \quad (60) \end{aligned}$$

The particle number density, N , was assumed to be constant. In order to obtain the energy equation of the large eddy, equation 60 was multiplied by u_i' :

$$\begin{aligned} & u_i' \frac{\partial}{\partial t}(u_i' + u_i'') + u_i' (U_x + u_x' + u_x'') \frac{\partial}{\partial x_x}(U_i + u_i' + u_i'') \\ &= -u_i' \frac{g_c}{\rho} \frac{\partial}{\partial x_i}(P + P' + P'') + u_i' \nu \frac{\partial^2}{\partial x_x \partial x_x}(U_i + u_i' + u_i'') \\ & \quad - u_i' \frac{KN}{\rho}(u_i' + u_i'') \quad (61) \end{aligned}$$

Expanding the left hand side of equation 61 and noting that:

$$\begin{aligned} & u_i' \frac{\partial}{\partial t}(u_i') + u_i' U_x \frac{\partial}{\partial x_x}(u_i') \\ &= \frac{\partial}{\partial t} \left(\frac{1}{2} u_i' u_i' \right) + U_x \frac{\partial}{\partial x_x} \left(\frac{1}{2} u_i' u_i' \right) = \frac{d}{dt} \left(\frac{1}{2} u_i' u_i' \right) \quad (62) \end{aligned}$$

then:

$$\begin{aligned} & \frac{d}{dt} \left(\frac{1}{2} u_i' u_i' \right) + u_i' \frac{\partial u_i''}{\partial t} + u_i' \frac{\partial}{\partial x_x} \left\{ (u_i' + u_i'')(u_x' + u_x'') \right\} \\ & + u_i' U_x \frac{\partial U_i}{\partial x_x} + u_i' u_x'' \frac{\partial U_i}{\partial x_x} + u_i' u_x' \frac{\partial U_i}{\partial x_x} + u_i' U_x \frac{\partial u_i''}{\partial x_x} \\ &= -u_i' \frac{g_c}{\rho} \frac{\partial}{\partial x_i}(P + P' + P'') + u_i' \nu \frac{\partial^2}{\partial x_x \partial x_x}(U_i + u_i' + u_i'') \\ & \quad - u_i' \frac{KN}{\rho}(u_i' + u_i'') \quad (63) \end{aligned}$$

Equation 63 was simplified by noting that:

$$U_L \frac{\partial U_i}{\partial x_L} + \frac{\partial}{\partial x_L} (\overline{u_i u_L}) = - \frac{g_c}{\rho} \frac{\partial P}{\partial x_i} + \nu \frac{\partial^2 U_i}{\partial x_L \partial x_L} \quad (64)$$

where $\overline{u_i u_L} = \overline{(u_i' + u_i'')(u_L' + u_L'')}$, was the equation for the mean motion and by noting that:

$$\frac{\partial U_L}{\partial x_L} = \frac{\partial u_L'}{\partial x_L} = \frac{\partial u_L''}{\partial x_L} = 0 \quad (65)$$

were the equations of continuity.

Then:

$$\begin{aligned} & \frac{d}{dt} \left(\frac{1}{2} u_i' u_i' \right) + u_i' \frac{\partial u_i''}{\partial t} + u_i' \frac{\partial}{\partial x_L} \left\{ (u_i' + u_i'')(u_L' + u_L'') - \overline{u_i u_L} \right\} \\ & + u_i' u_L' \frac{\partial U_i}{\partial x_L} + u_i' \frac{\partial}{\partial x_L} (u_i'' U_L + u_L'' U_i) \\ & = -u_i' \frac{g_c}{\rho} \frac{\partial P'}{\partial x_i} - u_i' \frac{g_c}{\rho} \frac{\partial P''}{\partial x_i} + \nu \left[\frac{\partial}{\partial x_L} \left(u_i' \frac{\partial u_i'}{\partial x_L} \right) - \left(\frac{\partial u_i'}{\partial x_L} \right) \left(\frac{\partial u_i'}{\partial x_L} \right) \right] \\ & - u_i' \frac{KN}{\rho} (u_i' + u_i'') + u_i' \nu \frac{\partial^2}{\partial x_L \partial x_L} (u_i'') \end{aligned} \quad (66)$$

Equation 66 was integrated over all space (essentially over one Townsend-Bakewell eddy):

$$\begin{aligned} - \int u_i' u_L' \frac{\partial U_i}{\partial x_L} dV &= \int u_i' \frac{\partial}{\partial x_L} \left\{ (u_i' + u_i'')(u_L' + u_L'') - \overline{u_i u_L} \right\} dV \\ &+ \nu \int \left(\frac{\partial u_i'}{\partial x_L} \right) \left(\frac{\partial u_i'}{\partial x_L} \right) dV + \frac{KN}{\rho} \int u_i' u_i' dV \end{aligned} \quad (67)$$

Note that the terms:

$$\int u_i' \frac{\partial u_i''}{\partial t} dV, \quad \int u_i' \frac{\partial}{\partial x_L} (u_i'' U_L + u_L'' U_i) dV,$$

$$\frac{KN}{\rho} \int u_i' u_i'' dV, - \int u_i' \frac{g_c}{\rho} \frac{\partial p''}{\partial x_i} dV, \int u_i' \nu \frac{\partial^2}{\partial x_\ell \partial x_\ell} (u_i'') dV$$

were dropped because the scales of the large eddies and the small eddies were assumed not to be correlated. Space integrals of products of large scale and small scale fluctuations would be negligible.

The terms: $-\frac{g_c}{\rho} \int u_i' \frac{\partial p'}{\partial x_i} dV$ and $\nu \int \left[\frac{\partial}{\partial x_\ell} (u_i' \frac{\partial u_i'}{\partial x_\ell}) \right] dV$ were dropped because the flow field was bounded and the flow of turbulent energy vanished at the wall. The term $\frac{g_c}{\rho} \int u_i' \frac{\partial p'}{\partial x_i} dV$ can be transformed, using the divergence theorem, into:

$$\frac{g_c}{\rho} \int u_i' \frac{\partial p'}{\partial x_i} dV = \frac{g_c}{\rho} \int u_2' p' ds - \frac{g_c}{\rho} \int p' \frac{\partial u_i'}{\partial x_i} dV \quad (68)$$

$g_c/\rho \int u_2' p' ds$ vanished because $u_2' p'$ was zero at the surface (The divergence theorem requires that only the normal component, u_2' , to the surface be considered in the surface integral); and $\frac{g_c}{\rho} \int p' \frac{\partial u_i'}{\partial x_i} dV$ vanished because of continuity.

The term $\nu \int \frac{\partial}{\partial x_\ell} (u_i' \frac{\partial u_i'}{\partial x_\ell}) dV$ can be transformed:

$$\begin{aligned} \nu \int \frac{\partial}{\partial x_\ell} (u_i' \frac{\partial u_i'}{\partial x_\ell}) dV &= \nu \int \left(\frac{\partial u_i'}{\partial x_\ell} \right) \left(\frac{\partial u_i'}{\partial x_\ell} \right) dV + \nu \int u_i' \frac{\partial^2 u_i'}{\partial x_\ell \partial x_\ell} dV \\ &= \nu \int \left(\frac{\partial u_i'}{\partial x_\ell} \right) \left(\frac{\partial u_i'}{\partial x_\ell} \right) dV + \nu \int \frac{\partial^2}{\partial x_\ell \partial x_\ell} \left(\frac{1}{2} u_i' u_i' \right) dV - \nu \int \left(\frac{\partial u_i'}{\partial x_\ell} \right) \left(\frac{\partial u_i'}{\partial x_\ell} \right) dV \end{aligned} \quad (69)$$

The remaining term $\nu \int \frac{\partial^2}{\partial x_\ell \partial x_\ell} \left(\frac{1}{2} u_i' u_i' \right) dV$ was identically zero:

$$\nu \int \frac{\partial^2}{\partial x_\ell \partial x_\ell} \left(\frac{1}{2} u_i' u_i' \right) dV = \nu \int \frac{\partial}{\partial x_2} \left(\frac{1}{2} u_i' u_i' \right) dS = \nu \int \frac{\partial}{\partial x_2} \left(\frac{1}{2} u_3'^2 \right) dS \quad (70)$$

which vanished at the surface (again considering only the normal component and also that $u_1' = u_2' = 0$ at the surface).

The term $d/dt \int \frac{1}{2} u_i' u_i' dV$ was dropped from equation 66 because it was assumed that large eddy was in a state of equilibrium. Equation 67, therefore, represented an integral energy balance for the Townsend-Bakewell eddy. The gain of energy from the mean flow, the left hand side of the equation, was exactly balanced by the three part dissipation on the right hand side of the equation. The dissipation was composed of (1) energy loss to the smaller eddies (additional Reynolds stress) (2) direct viscous dissipation and (3) additional dissipation produced by the presence of particles. The additional Reynolds stress was calculated from:

$$\overline{u_i u_\ell} - (u_i' + u_i'')(u_\ell' + u_\ell'') = 2 \nu_T S_{i\ell}' \quad (71)$$

where $\nu_T = x_2 U_* / R_v$. The diagonal components of $S_{i\ell}'$ were ignored (Townsend 1956:120) so that

$$S_{i\ell}' = \begin{bmatrix} 0 & \frac{1}{2} \left[\frac{\partial u_2'}{\partial x_1} + \frac{\partial u_1'}{\partial x_2} \right] & \frac{1}{2} \left[\frac{\partial u_3'}{\partial x_1} + \frac{\partial u_1'}{\partial x_3} \right] \\ s_{12} & 0 & \frac{1}{2} \left[\frac{\partial u_3'}{\partial x_2} + \frac{\partial u_2'}{\partial x_3} \right] \\ s_{13} & s_{23} & 0 \end{bmatrix} \quad (72)$$

All of the terms in equation 67 were defined, so that it was possible to complete the integration of equation 67. The limits of the integrals were 0 to 1 for x_1 , 0 to infinity for x_2 , and negative infinity to positive infinity for x_3 .

The integration of the left-hand side of equation 67 was straightforward. Because it illustrates the mathematics, it will be shown below in detail:

$$\begin{aligned}
 -\int u_i' u_j' \frac{\partial U_i}{\partial x_j} dV &= -\int_0^1 \int_{-\infty}^{\infty} \int_0^{\infty} u_i' u_j' \frac{\partial U_i}{\partial x_j} dx_2 dx_3 dx_1 \\
 &= 2 \int_0^1 \int_0^{\infty} \int_0^{\infty} \frac{R_N U_*}{x_2} A^2 \alpha^4 x_2^2 x_3^2 e^{-\alpha^2 x_2^2} e^{-\alpha^2 x_3^2} dx_2 dx_3 dx_1 \\
 &= 2 R_N U_* A^2 \alpha^4 \int_0^1 dx_1 \int_0^{\infty} x_2 e^{-\alpha^2 x_2^2} dx_2 \int_0^{\infty} x_3^2 e^{-\alpha^2 x_3^2} dx_3 \\
 &= 2 R_N U_* A^2 \alpha^4 (1) \left(\frac{1}{2\alpha^2} \right) \left(\frac{1}{4\alpha^2} \sqrt{\frac{\pi}{\alpha^2}} \right) = \frac{\sqrt{\pi} A^2 U_*}{4\alpha} R_N
 \end{aligned} \tag{73}$$

The integration of the right-hand side involved multiple use of the exponential integral formulae and was extremely lengthy because of the many summations, differentiations, and multiplications required.

The integration will be shown below in outline form only. For the Reynolds stress term:

$$\begin{aligned}
 \int u_i' \frac{\partial}{\partial x_j} \{ (u_i' + u_i'')(u_j' + u_j'') - \overline{u_i u_j} \} dV \\
 &= \int u_i' \frac{\partial}{\partial x_j} \left(2 \frac{U_* x_2}{R_N} S_{ij}' \right) dV \\
 &= -4 \frac{U_*}{R_N} \int_0^{\infty} \int_0^{\infty} u_i' \frac{\partial}{\partial x_j} \{ x_2 S_{ij}' \} dx_2 dx_3
 \end{aligned} \tag{74}$$

This term was simplified by noting that the diagonal terms of the strain tensor were to be ignored and that the u_i' were not functions of x_1 .

$$\begin{aligned} \therefore -4 \frac{U_*}{R_N} \int_0^\infty \int_0^\infty u_i' \frac{\partial}{\partial x_i} \{x_2 S_{i1}'\} dx_2 dx_3 \\ = -4 \frac{U_*}{R_N} \int_0^\infty \int_0^\infty \left[u_1' \frac{\partial}{\partial x_2} \{x_2 S_{12}'\} + u_1' \frac{\partial}{\partial x_3} \{x_2 S_{13}'\} \right. \\ \left. + u_2' \frac{\partial}{\partial x_3} \{x_2 S_{23}'\} + u_3' \frac{\partial}{\partial x_2} \{x_2 S_{32}'\} \right] dx_2 dx_3 \end{aligned} \quad (75)$$

After substitution of the strain tensor components and the equations for the large eddy velocity components and after completing the integration the Reynolds stress term became:

$$\begin{aligned} \frac{U_* A^2 \sqrt{\pi}}{\alpha R_N} \frac{1}{4} + \frac{U_* A^2 \sqrt{\pi}}{\alpha R_N} \frac{3}{8} + \frac{U_* A^2 \sqrt{\pi}}{\alpha R_N} \frac{1}{8} + \frac{U_* A^2 \sqrt{\pi}}{\alpha R_N} \frac{10}{8} \\ = 2 \sqrt{\pi} \frac{A^2 U_*}{\alpha R_N} \end{aligned} \quad (76)$$

For the viscous dissipation term:

$$\begin{aligned} \nu \int \left(\frac{\partial u_i'}{\partial x_i} \right) \left(\frac{\partial u_i'}{\partial x_i} \right) dV = 2\nu \int_0^\infty \int_0^\infty \left(\frac{\partial u_i'}{\partial x_i} \right) \left(\frac{\partial u_i'}{\partial x_i} \right) dx_2 dx_3 \\ = 2\nu \int_0^\infty \int_0^\infty \left[\left(\frac{\partial u_1'}{\partial x_2} \right) \left(\frac{\partial u_1'}{\partial x_2} \right) + \left(\frac{\partial u_1'}{\partial x_3} \right) \left(\frac{\partial u_1'}{\partial x_3} \right) + \left(\frac{\partial u_2'}{\partial x_2} \right) \left(\frac{\partial u_2'}{\partial x_2} \right) \right. \\ \left. + \left(\frac{\partial u_2'}{\partial x_3} \right) \left(\frac{\partial u_2'}{\partial x_3} \right) + \left(\frac{\partial u_3'}{\partial x_2} \right) \left(\frac{\partial u_3'}{\partial x_2} \right) + \left(\frac{\partial u_3'}{\partial x_3} \right) \left(\frac{\partial u_3'}{\partial x_3} \right) \right] \end{aligned} \quad (77)$$

After substitution of the large eddy velocity components, the viscous dissipation term became:

$$\begin{aligned} \frac{3}{16} \pi \nu A^2 + \frac{3}{16} \pi \nu A^2 + \frac{3}{16} \pi \nu A^2 + \frac{3}{16} \pi \nu A^2 \\ + \frac{15}{16} \pi \nu A^2 + \frac{3}{16} \pi \nu A^2 = \frac{15}{8} \pi \nu A^2 \end{aligned} \quad (78)$$

For the particle dissipation term:

$$\begin{aligned}
 \frac{KN}{\rho} \int u_i' u_i' dV &= \frac{2KN}{\rho} \int_0^1 \int_0^\infty \int_0^\infty [u_1' u_1' + u_2' u_2' + u_3' u_3'] dx_2 dx_3 dx_1 \\
 &= 2 \frac{KN}{\rho} \int_0^\infty \int_0^\infty A^2 e^{-\alpha^2(x_2^2 + x_3^2)} [2\alpha^4 x_2^2 x_3^2 + 1 - 2\alpha^2 x_2^2 \\
 &\quad + \alpha^4 x_2^4] dx_2 dx_3 \quad (79) \\
 &= 2 \frac{KN}{\rho} \left[\frac{A^2 \pi}{16\alpha^2} + \frac{A^2 \pi}{16\alpha^2} + \frac{3A^2 \pi}{16\alpha^2} \right] = \frac{5\pi}{8} \frac{KN A^2}{\rho \alpha^2}
 \end{aligned}$$

The final result of the full integration was:

$$\frac{\sqrt{\pi} A^2 U_*}{4\alpha} R_N = \frac{2\sqrt{\pi} A^2 U_*}{\alpha R_N} + \frac{15}{8} \pi \nu A^2 + \frac{5\pi KN A^2}{8 \rho \alpha^2} \quad (80)$$

$$\text{Assuming } 1/\alpha = \delta_N^+ \quad (81)$$

and noting that:

$$\eta = \frac{\pi}{6} a^3 N \frac{\rho_p}{\rho} \quad , \quad t_R = \frac{1}{18} \frac{a^2}{\nu} \frac{\rho_p}{\rho} \quad ,$$

$$\text{and } \frac{KN}{\rho} = \frac{\eta}{t_R}$$

for equal particle and air bulk average velocities, equation 80 was solved explicitly for the dimensionless sublayer thickness:

$$R_N = \sqrt{\frac{8(1 + \frac{15}{16}\sqrt{\pi})}{(1 - \frac{5\sqrt{\pi}}{2} \frac{\nu}{U_*^2} \frac{\eta}{t_R})}} \quad (82)$$

Jotaki and Tomita did not use equation 82 to establish a relationship between sublayer thickness and the amount of drag reduction. They did use the assumptions of their analysis to determine the conditions for which drag reduction could be expected. Use of the Stokes drag formula required

that the particle Reynolds number be less than unity:

$$\frac{(u_i' + u_i'')a}{\nu} \approx \frac{2aU_*}{\nu} < 1 \quad (83)$$

The assumption that the particles had no fluctuating component of velocity required that the relaxation time of the particles be longer than the characteristic time of the flow in the wall region:

$$\frac{1}{18} \frac{a^2}{\nu} \frac{\rho_p}{\rho} > \frac{\nu}{U_*^2} \quad (84)$$

The assumption that the mean particle velocity be the same as the mean gas velocity required that the terminal velocity of the particles be smaller than the characteristic velocity of the flow in the wall region:

$$\frac{1}{18} \frac{a^2}{\nu} \frac{\rho_p}{\rho} g < U_* \quad (85)$$

Combination of the equations 83 through 85 and the additional requirement that R_v in equation 82 be real gave the following requirements for flow parameters in a drag reducing suspension:

$$\eta < \frac{1}{180\sqrt{\pi}} \frac{\rho_p}{\rho} \quad (86)$$

$$\frac{\rho}{\rho_p} < \frac{1}{72} \quad (87)$$

$$a < \frac{1}{2} \sqrt[3]{\frac{\nu^2}{g}} \quad (88)$$

For glass beads in air, the inverse of the density ratio is 2130. The maximum loading ratio and bead size for drag reduction would, therefore, be 6.68 and 142 μ respectively. These values are reasonable and include the range of parameters investigated here and in most other studies. However, some investigators have reported drag reduction with higher loading ratios and larger particle sizes. Jotaki and Tomita indicated that the conditions of equations 83 through 85 may be too restrictive. They felt that drag reduction would occur as long as the additional dissipation produced by the presence of particles outweighed any additional turbulent energy production (produced possibly by disturbances from large particle wakes or by an increased effective fluid density).

MODIFICATION AND EXTENSION OF THE THEORY OF JOTAKI AND TOMITA

Jotaki and Tomita did not develop a relationship between the dimensionless sublayer thickness, R_v , and the friction factor. This relationship was obtained using the combined velocity profile of the viscous sublayer and buffer layer (equation 55) and the relationship:

$$U_* = U \sqrt{\frac{f}{8}} \quad \text{or} \quad f = \left(\frac{U_*}{U} \right)^2 \quad (89)$$

where U was the bulk average gas velocity and was a constant for a given gas Reynolds number. (Note that U in equation 55 was the local mean velocity which varied continuously from the wall.)

In order to use the velocity profile (equation 55) as a

relationship between R_v and U_* , a point of the buffer layer velocity profile was required. This point was available from the experimentally measured velocity profiles. In particular, the traverse point nearest the wall, at $y/R = 0.078$, was within the buffer layer. Using the velocity here, equation 55 defined the variation of R_v with U_* . The basic assumption of this newly defined relationship was that the presence of particles did not destroy the validity of the logarithmic law. This assumption had been well justified by the suspension air velocity profiles.

Figure 56 shows the relationship of R_v and U_* calculated for Reynolds numbers of 12,000; 18,000; and 22,000. The values of R_v corresponding to the values of U_* determined by the clean gas pressure drop measurements are shown by the dashed curve. The curve has been extended to a Reynolds number of 50,000 using a value of U_* calculated from the experimental friction factor correlation. In the range $12,000 < Re_g < 50,000$, the value of R_v is seen to vary only slightly for the clean gas. At a Reynolds number of 18,000, the value of R_v (4.61) predicted theoretically by equation 82 for the clean gas was in exact correspondence with the value of R_v calculated from the experimental data and equation 55.

Figure 56 showed that larger dimensionless sublayers produced smaller friction velocities (and, therefore, friction factors) and confirmed analytically that thickened sublayers can cause drag reduction. On the one hand, the figure showed that the amount of drag reduction was limited as the sublayer thickened to the order of quadruple the original thickness. On the other hand, the amount of drag increase by sublayer thinning was not limited.

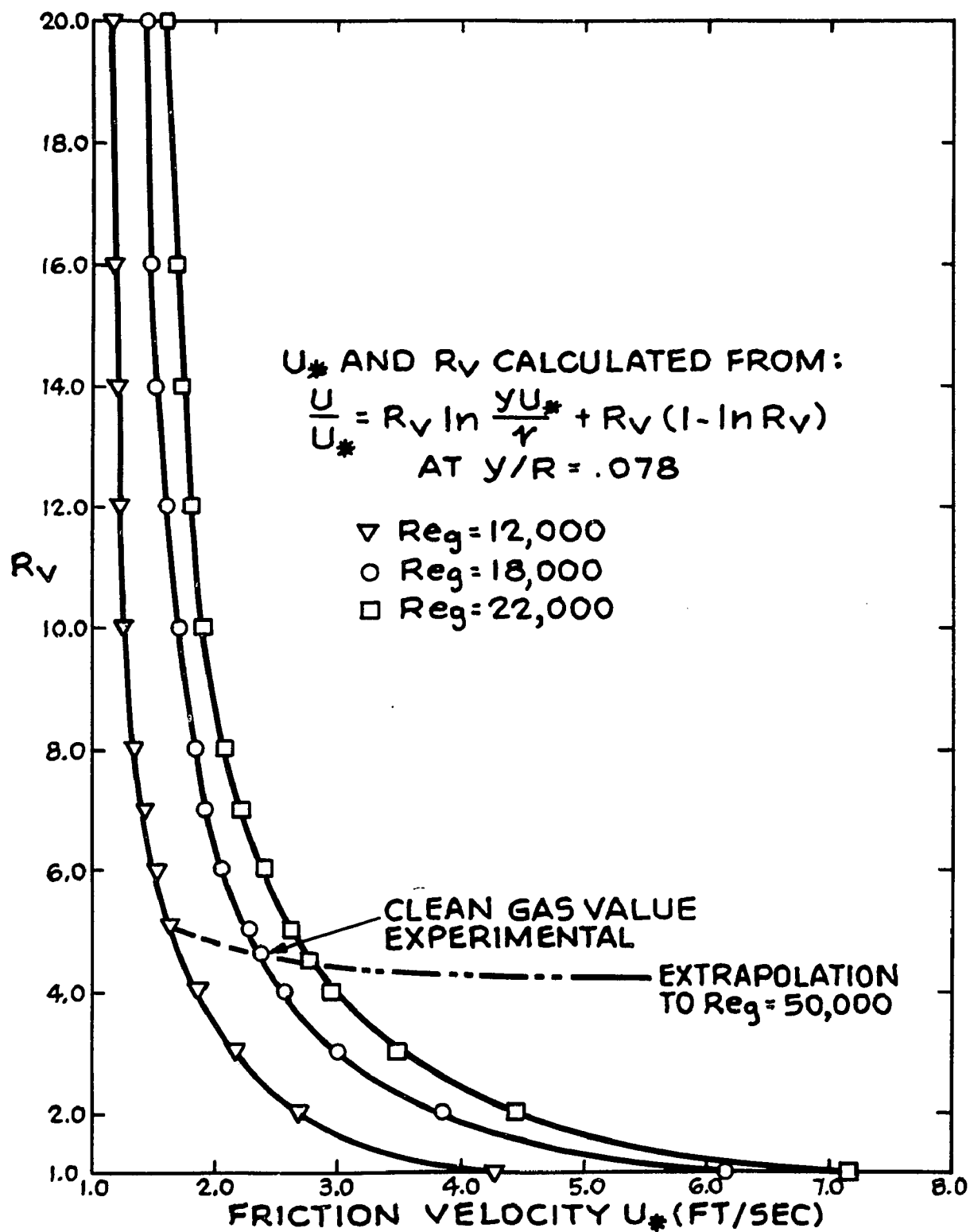


Figure 56

Dimensionless Sublayer Thickness
 as a Function of Friction Velocity

Figure 57 shows the information of Figure 56 recast in the form of friction factor ratio versus dimensionless sublayer thickness ratio for the range of Reynolds numbers considered in this study. In this form, the figure showed that the friction factor ratio was a very weak function of Reynolds number, in exact correspondence with the experimental results of Chapter 5. The amount of drag reduction was limited to about 50 percent for reasonable values of the dimensionless sublayer thickness ratio. This was also in accordance with the results of Chapter 5.

Because the friction factor ratio was essentially independent of Reynolds number, as shown in Figure 57, a theoretical prediction of the amount of drag reduction needed to consider only one consistent set of values of R_v and U_* . The exact correspondence of the theoretically and experimentally derived values of R_v for a Reynolds number of 18,000 obliged the use of this set of values.

Equation 82 and Figure 57 were used to compare the experimentally measured amounts of drag reduction for a particular loading ratio and particle size with theoretical predictions for friction factors. Equation 82 provided an unsatisfactory prediction as theoretical loading ratios in excess of those considered valid by the assumptions of the analysis were required to produce the amount of sublayer thickening indicated by the experiments. Consequently the analysis was modified to account for two factors, the first enhancing drag reduction, the second limiting drag reduction, which were not considered in the original analysis. The two factors were both related to the most questionable assumption in the Jotaki and Tomita analysis; that the particles were at the same time completely responsive to the mean flow and

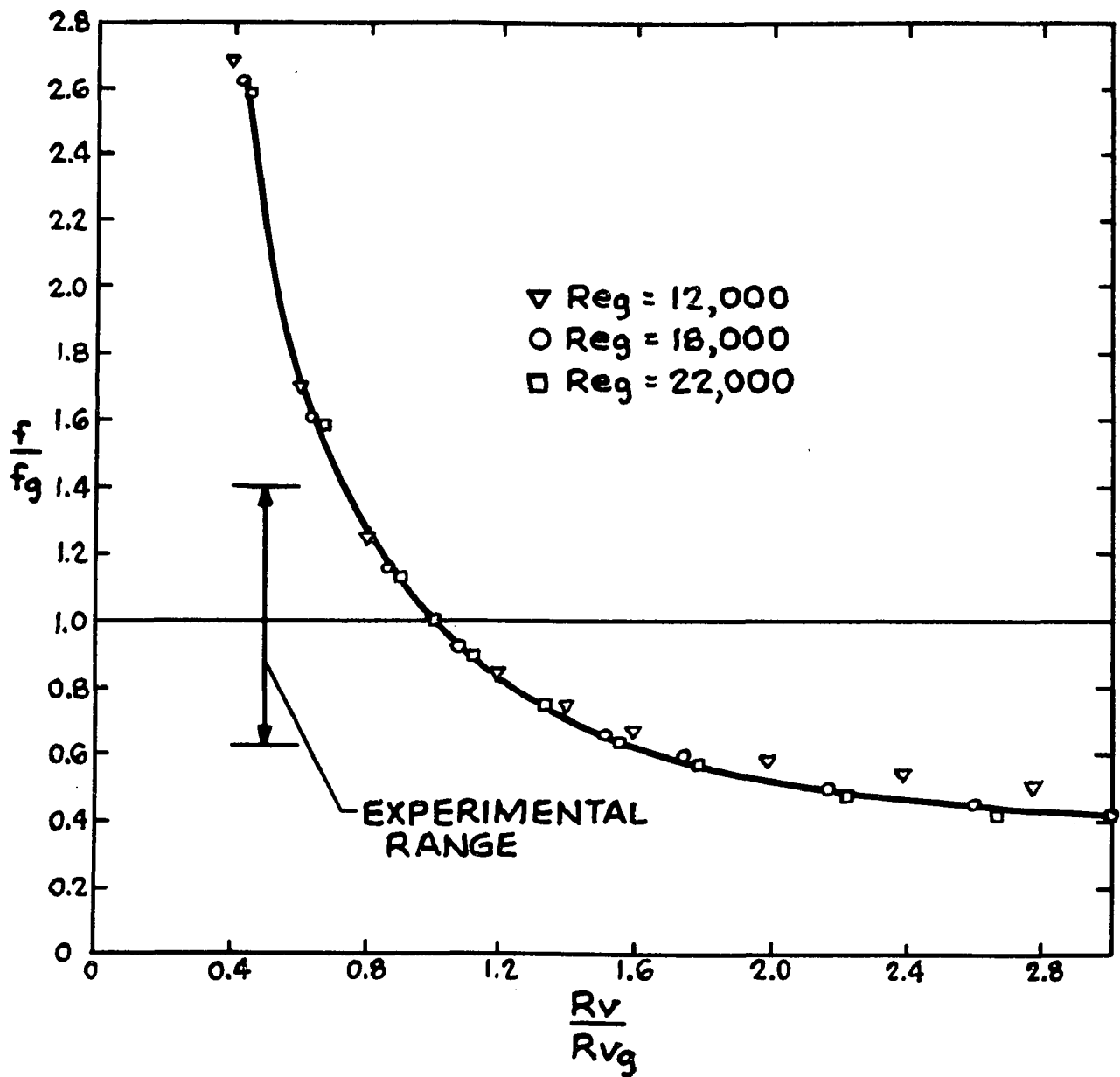


Figure 57
Friction Factor Ratio Versus
Dimensionless Sublayer Thickness Ratio

totally unresponsive to the turbulent fluctuations of the gas.

The first factor, c_1 , was introduced into the final term of equation 48 to correct the relative velocity between the solids and the gas. The factor represented the ratio between the velocity of the particles at the wall (which do not follow the no-slip boundary condition) and the characteristic velocity of the Townsend-Bakewell eddy. The factor could be interpreted in two ways. First it accounted for the higher actual relative velocity and increased dissipation between the two phases in the near wall region. Second, it represented the effectively higher loading ratio in the thin sub-layer region because the particles were travelling faster than the gas.

According to Bakewell and Lumley (Bakewell and Lumley 1967), the characteristic velocity of the large eddy was:

$$A \approx 0.7U_* \quad (90)$$

Reddy and Pei (Reddy and Pei 1969) suggested that the particle velocity at the wall could be estimated by:

$$v_{pw} = v_{pc} \left(\frac{a}{D}\right)^{1/n} \quad (91)$$

The value of n , calculated from the data of Reddy and Pei was 10.42 for 200 μ particles. Extrapolating this value from the data of Reddy and Pei with 100 μ , 150 μ , 200 μ , and 270 μ particles to the 36.0 μ particles used here gave a value of 9.38 for n . Equation 91, therefore, gave a value of v_{pw} of 25.25 ftsec⁻¹ for the #380 glass beads at a Reynolds number of 18,000 in the 0.870 inch inside diameter tube.

In the 1 inch inside diameter glass tube, the particle

velocities measured nearest the wall (the camera was actually focused at the wall) were of the order of 21 ftsec^{-1} for tube centerline velocities of 37.5 ftsec^{-1} which was considered as the equivalent of 28 ftsec^{-1} for the tube centerline velocities of 50 ftsec^{-1} (corresponding to a Reynolds number of 18,000) in the 0.870 inch inside diameter tube. The particle velocity measured nearest the wall was considered as an upper estimate for the velocity at the wall since the depth of field of the camera was thicker than the viscous sublayer. Because the camera was focused at the wall, this estimate was probably close to the actual velocity at the wall.

Therefore, using the Reynolds number of 18,000 in the 0.870 inch inside diameter tube, the factor c_1 was chosen as:

$$c_1 = \frac{V_{pw}}{A} \approx \frac{25.25}{0.7U_*} = \frac{25.25}{0.7(2.38)} = 15.15 \approx 15. \quad (92)$$

Unfortunately experimental data was not available to estimate c_1 for the #279 and #980 glass beads. However, c_1 was estimated as 5.40 and 2.60 for the #279 and #980 glass beads by considering the relative relaxation times of the various glass bead sizes. The relative relaxation times were assumed to be the best measure of the relative response of the particles to the mean flow and, therefore, were used for the extrapolation of c_1 to very small particle sizes.

The second factor, c_2 , was introduced in order to account for the proportionate increase in turbulent production produced by the presence of particles. Calculations of the response of a particle to a given turbulent spectrum (homogeneous and isotropic) have been made and reported (Soo 1967:31-43). In the work reported by Soo, the response of

50 micron and 5 micron carbon particles to a given turbulent air spectrum corresponding to the centerline of a pipe was calculated. The results were plotted in terms of relative energy, compared to the energy of the air at a frequency of $10 \text{ cyclessec}^{-1}$, versus frequency. The area under the energy-frequency curve was a measure of the turbulence intensity. The area under the curve for the 50 micron particles was 0.082 of the area under the curve for the air indicating that the particles were effectively responsive to 8.2% of air turbulence. For high frequencies, which are typical of the turbulence near the wall, the smaller 5 micron particles were two orders of magnitude more responsive than the 50 micron particles.

In order to estimate c_2 for the #380 glass beads in the near wall region, the response of the 50μ carbon particles to the air turbulence at the center of a pipe was used as a base point. The relative response of the 50μ and 5μ particles to the small eddies or high frequency turbulence implied that the response was, roughly, inversely proportional to the square of the particle diameter. This suggested that the #380 glass beads would be effectively responsive to $(\frac{50}{36})^2 (0.082)$ or 0.162 of the air turbulence provided the scale of the average eddy at the centerline was the same as that in the near wall region.

However, the scale of the average eddy was much smaller near the wall than at the centerline. A measure of the relative average eddy size between the pipe centerline and the near wall region was provided by the relative magnitude of the mixing length between the pipe centerline and the near wall region. From previous investigations (Schlichting 1968:569), the mixing length in the region $y/R < 0.1$ is one

order of magnitude smaller than at the pipe centerline. Therefore, scaling the factor c_2 by the relative size of the average eddy gave a value of 0.0162 for the #380 glass beads. Because the approximation was crude, c_2 was rounded to 0.02 for the #380 glass beads. The magnitude of c_2 for the other particle sizes was scaled by the squares of the particle diameters, therefore, c_2 was chosen as 0.056 and 0.115 for the #279 and #980 glass beads respectively. Therefore, an equal (to the gas flow rate) flow rate of #380 glass beads was considered to add only another 2 per cent to the turbulent production of the gas. The #980 glass beads, being so much smaller and more responsive to the flow were considered to add another 11.5 per cent to the turbulent production of the gas.

The factor, c_2 , was included in equation 48 by multiplying the density of the fluid by $1 + c_2 \eta$. Equation 48 with c_1 and c_2 included became:

$$\begin{aligned} \rho(1+c_2\eta) \left(\frac{\partial u_i}{\partial t} + u_\ell \frac{\partial u_i}{\partial x_\ell} \right) = & -g_c \frac{\partial p}{\partial x_i} \\ & + \rho(1+c_2\eta) \nu \frac{\partial^2 u_i}{\partial x_\ell \partial x_\ell} \\ & + c_1 K N (N_i - u_i) \end{aligned} \quad (93)$$

The integration of this equation followed as before so that the equation for R_v became:

$$R_N = \sqrt{\frac{8(1 + \frac{15}{16} \frac{\sqrt{\pi}}{1+c_2\eta})}{1 - \frac{5\sqrt{\pi}}{2} \frac{\nu}{u_*^2} \frac{\eta}{t_R} \frac{c_1}{1+c_2\eta}}} \quad (94)$$

Equation 94 and Figure 57 were used to calculate the results

shown in Figure 58 where friction factor ratio is plotted versus loading ratio for the #380 glass beads with $c_1 = 15$ and two values of c_2 .

The sensitivity of the solution to small changes in the value of c_2 of the order of 0.1 is readily apparent. Changing the turbulent production factor c_2 from 0 to 0.1 has the effect of increasing the loading ratio for a given amount of drag reduction by about 40 percent. The figure implied that if c_2 were increased to values near unity little drag reduction could be expected at reasonable values of loading ratio.

Figure 59 shows the theoretical prediction of friction factor ratio versus loading ratio for the #980, #279 and #380 glass beads. The agreement with the experimental results of Chapter 5 was quite good. At the highest values of loading ratio, the limiting value of drag reduction observed experimentally could be predicted adequately by assuming a very small additional bulk slip velocity of the order of 25 per cent of the particle terminal settling velocity. Equation 3 was used to show that a bulk average particle slip velocity only 27% greater than the particle terminal settling velocity could increase the friction factor ratio of the #380 glass bead suspension by 15 per cent. This result is shown on Figure 59 and brings the theoretically predicted curve into close agreement with the experimental data.

The bulk average particle slip velocity could be expected to increase at higher loading ratios and particle number densities because of increased particle-particle and particle-wall impacts. The small additional slip velocity, only 0.088 ftsec^{-1} for the #380 glass beads, was well within the precision limits of the particle velocity measurement system.

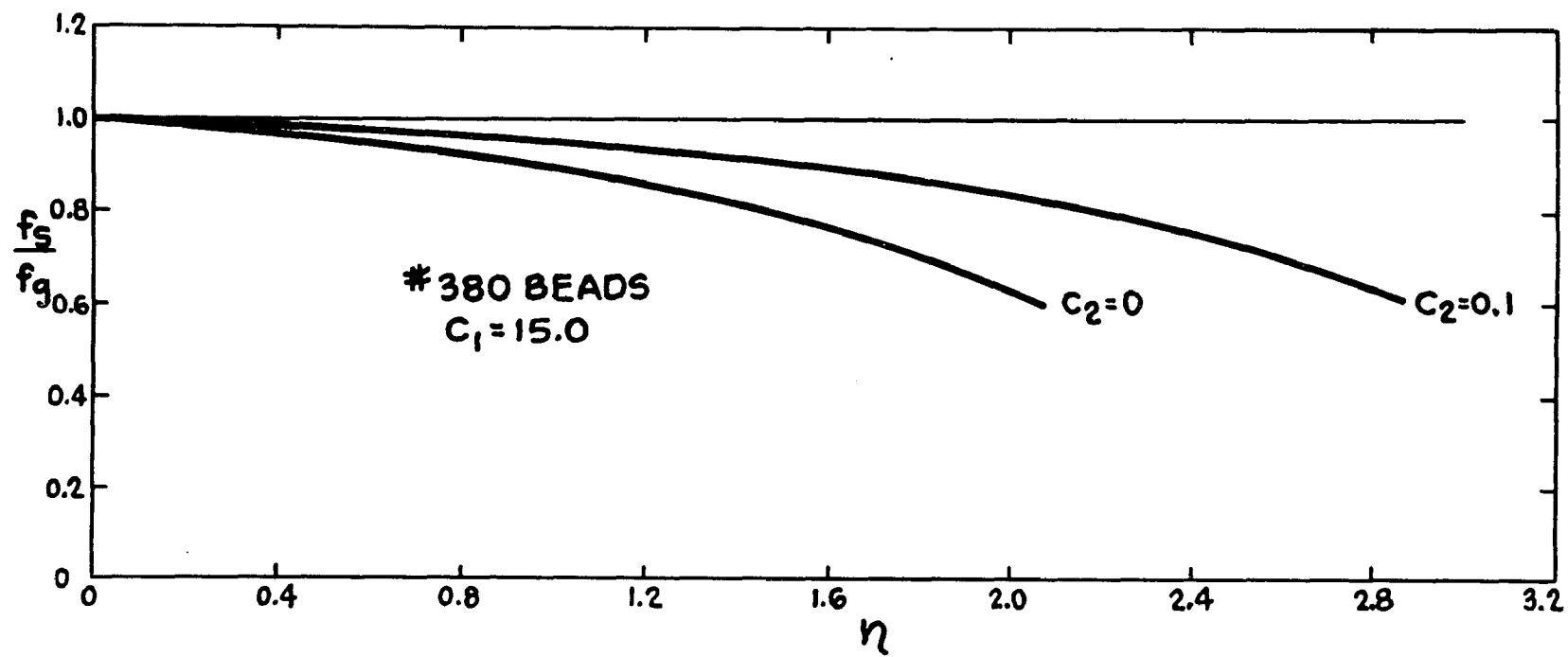


Figure 58 Friction Factor Ratio Versus Loading Ratio Based upon Modified Analysis with Variable Production

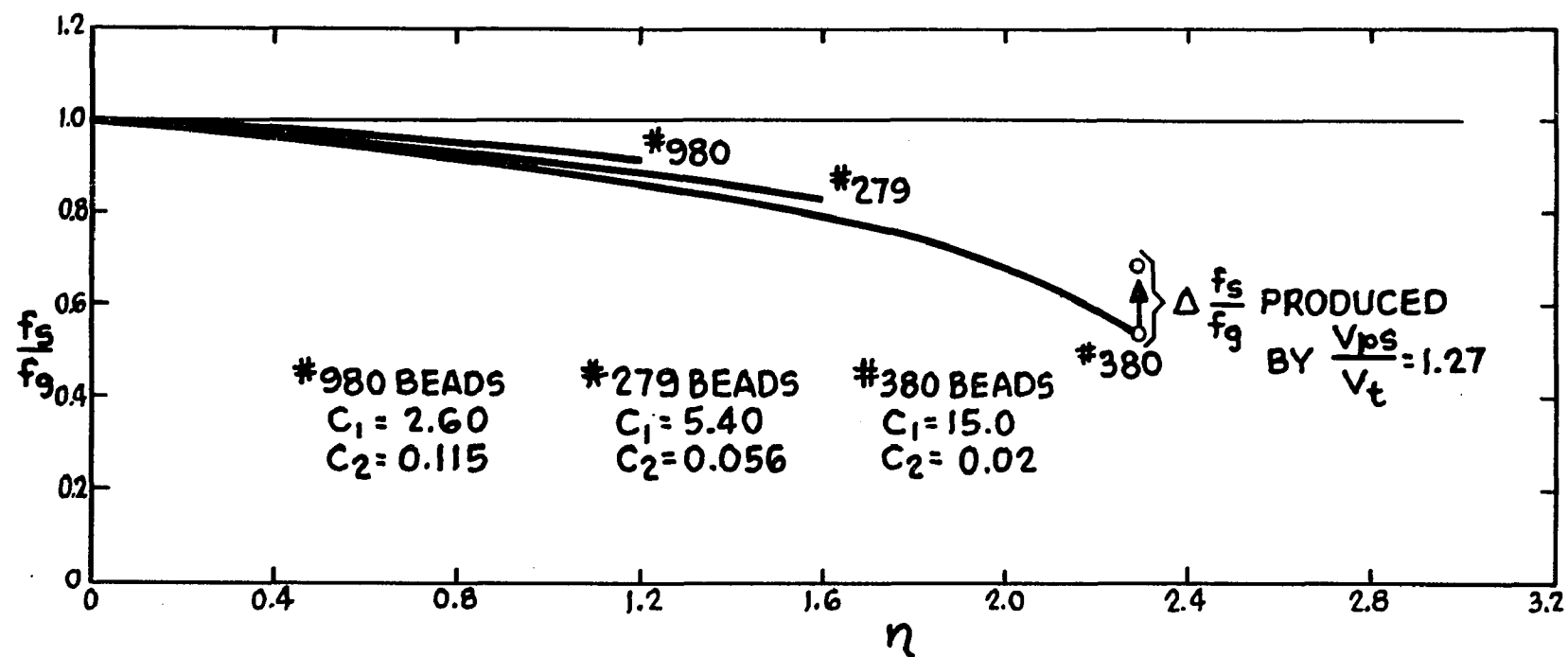


Figure 59 Friction Factor Ratio Versus Loading Ratio Based upon Modified Analysis with Variable Particle Size

Chapter 7

RECOMMENDATIONS FOR FUTURE WORK

The three phase investigation into the existence, causes, and effects of drag reduction in dilute flowing gas-solids suspensions was intended to be thorough but not exhaustive. The program certainly confirmed the existence of drag reduction and determined the outward causes of the phenomenon. However, more detail on the structure of turbulent tube flow in the presence of a drag-reducing suspension, particularly in the near wall region, will be required to completely describe the drag reduction mechanism.

Although the measurement of many of the flow properties obliged development of unique measurement techniques and unorthodox utilization of the latest experimental apparatus, the inherent limitations of the current technology prevented accumulation of data on the finer details of the problem. Until the experimental art is developed past its present state, some of the more sophisticated theories, existent or developing, of gas-solids drag reduction will have to be regarded as tentative opinions awaiting experimental evidence. This chapter provides suggestions for further experimental and theoretical work with the implication that the two aspects of the problem are of uniform and complementary value.

EXPERIMENTAL

The most serious limitation of this study, the inability to accurately measure turbulence quantities in the near wall region, was not unique to the flow of gas-solids suspensions. However, the abrasive nature of the medium prevented use of standard techniques such as hot-wire anemometry. Fortunately a new device, a laser Dopplermeter, which does not require insertion of any probe whatsoever into the stream will soon be commercially available. Perhaps the laser Dopplermeter will enable measurement of mean velocities and turbulence intensities in the viscous sublayer without concern about external disturbances to the flow pattern. If the new device proves successful in measuring longitudinal turbulence intensities in the sublayer, the measurement of other turbulence quantities such as transverse turbulence intensities, and Eulerian and LaGrangian correlations, may also be possible. In fact the operational characteristics of the device may also permit accurate measurement of particle velocities in the sublayer and particle turbulence intensities throughout the entire tube cross-section.

A commercially available spectrum analyzer featuring low noise levels and high resolution at low frequencies could be included in the laser Dopplermeter circuitry to provide a more informative spectrum analysis than was obtained here. A mini-computer and a permanent tape recorder would be recommended as an integral part of the spectral analysis system. Assuming the laser Dopplermeter and spectrum analyzer operate as efficiently as claimed by their manufacturers, (laser techniques have been used successfully in liquids (Patterson and Zakin:1971)) a complete description of the turbulence characteristics of both the gas and solids

could be obtained. This description would be of inestimable value in formulating theories of gas-solids drag reduction based on mutual accommodation of gas and solids.

The revised anemometry system could also be used to measure gas and particle velocity profiles in a horizontal flow, something which was not attempted here because of space, time, and cost limitations. Profile measurements in the vertical test section required the sacrifice of approximately one dozen costly and unrepairable probes. However, only one traverse across the tube diameter was sufficient to describe the entire flow field because of radial symmetry. The same symmetry did not exist in the horizontal test section; and, the number of traverses required to describe the flow pattern would have lead to exorbitant probe costs. This problem would be avoided by the use of a laser Dopplermeter since probes are not required.

There are other measurements which could be made to extend the range of the measurements made during this study. The tube diameter could be changed in order to confirm the observations of workers in both gaseous and liquid drag reduction that drag reduction increases with decreasing tube diameter (Boothroyd 1971:130; Davies 1972:288-289). Changing the tube diameter would also afford an opportunity of determining if the observed weak effect of Reynolds number on drag reduction was limited to the range of Reynolds numbers considered during this study. The effect of particle attrition on the strain gauge calibration and on the pressure drop measurements could be isolated by limiting particle abrasion. This admittedly would be difficult to do since it would involve increasing the pump clearances to permit smoother passage of large particle sizes.

The heat transfer measurements could be extended to measurement of longitudinal wall and bulk temperature profiles and to calculation of local heat transfer coefficients. The tendency of small particles (Depew and Farbar 1963) to delay the establishment of a fully developed temperature profile could be verified. The suggested modifications to the pump would permit circulation of large diameter particles and, with the information obtained on the effect of attrition on pressure drop measurements, add to the report (Farbar and Depew 1963) that uniformly sized as opposed to diversely sized particles are required for Nusselt number reduction.

THEORETICAL

Although, a theoretical prediction of the amount of sublayer thickening produced by a given particle size and concentration can be postulated, a full analysis of the mutual interaction between the particles and gas turbulence has not been developed. Most theories of the turbulent structure of a suspension consider one of two extremes. The particles are either treated as individuals responding to an unchangeable fluid turbulent structure (Hinze 1959:353-359) or as part of a continuous fluid of uniform density (Owen 1969).

The conditions of gas-solids drag reduction fall somewhere between the two extremes. The measurements of this study proved conclusively that particles affect the turbulent structure of the fluid so that the fluid turbulence cannot be considered independently of the presence of particles. The wide dispersion of individual particles of the dilute suspension suggested that treatment as a denser fluid is not

valid. The correct theoretical treatment will be determined only after future measurements provide detailed information on the eddy structure of the fluid in the presence of particles.

Before the experimental information is available, some additional progress may be made toward a theoretical determination of the energy interaction between fluid turbulence and particles under equilibrium conditions. An approach of this sort may be used to refine the theory determining the effect of particles on the thickness of a quasi-equilibrium structure such as the viscous sublayer in a shear flow. As measurements of particle turbulence and slip velocity in the sublayer region become available, the theory may be modified more rigorously to account for the inertial effects of the finite particle slip velocity in the wall region. Like one-phase turbulent problems, the theoretical analysis of two-phase turbulence will inevitably require constants obtainable only from experimental tests.

The difficulty of the problem is best summarized by the following quotation from Boothroyd (Boothroyd 1971:196-197):

It was also seen ... that, although the ideal case of homogeneous isotropic turbulence was amenable to formal analysis, there was no indication that this could be applied to shear flow. Two main complications prevent this:

- (1) It is usually impossible for a particle to come to a state of equilibrium with the fluid turbulence in a boundary layer, because the fluid turbulence structure usually has too small a time-scale and varies enormously in a direction normal to the wall.
- (2) The turbulent properties of both phases are mutually dependent on each other, so that it is not possible to evaluate the properties of one phase directly by assuming those of the other.

For these reasons the nature of self-generated shear flow turbulence in fine-particle suspensions is shrouded in a great deal of mystery. It might perhaps be claimed that there is now just a faint glimmer of understanding in this subject.

Chapter 8

SUMMARY AND CONCLUSIONS

A three phase investigation into the existence, causes, and effects of gas-solids drag reduction was performed in a recirculating closed loop system. The three phases consisted of pressure drop, profile, and heat transfer measurements on suspensions in air or argon flowing in vertical and horizontal 0.870 inch I.D. tubes. The pressure drop and profile measurements included data, in the presence of particles, on gas friction factors, gas velocity profiles, gas turbulence intensity profiles, gas turbulent spectra, and particle velocity profiles. During the pressure drop and particle measurements, five glass bead sizes ranging from 10 to 60 μ diameter were suspended in air at gas Reynolds numbers of 10,000 to 25,000 and solids loading ratios from 0 to 4. The heat transfer work included data on argon-glass and argon-aluminum suspensions. The pressure drop and heat transfer measurements were taken in both horizontal and vertical test sections. The profile measurements were confined exclusively to the vertical test section.

The investigation, without reliance upon any experimental assumptions, quite definitely confirmed the existence of drag reduction and corresponding Nusselt number reduction. The experimental technique required extensive calibrations and internal consistency checks in order to confirm the accuracy of the results. Because of the harshness of the gas-solids

environment, standard procedures for measurement of gas turbulence quantities could not be used without modification. Novel and unusual procedures were developed for measuring gas properties as well as for determining the values of significant parameters such as gas and solids flow rates.

The profile measurements in particular required the use of equipment which had been described as completely unsuitable for suspension measurements. The successful use of the equipment provided information on the external causes of the gas-solids drag reduction. The results gave further credence to some theories and cast doubt upon some others.

This chapter concludes with an enumerated condensation of the important observations, results, and conclusions reported in earlier chapters of this dissertation:

- (1) The five glass bead designations #980 (15.0 μ), #981 (21.0 μ), #279 (21.6 μ), #380 (36.0 μ), #660 (55.0 μ), were sized before and after circulation in the closed loop system and were found, with the exception of the #660 glass beads, to retain their initial weight mean diameters. The #660 glass beads suffered a three-fold reduction in average size, apparently a consequence of prolonged passage through the pump. The H-322 (30.0 μ) aluminum powder could not be sized after circulation because of the existence of large aluminum agglomerates which continually plugged the aperture of the Coulter Counter used for size distribution analyses.
- (2) The closed loop system successfully resisted the abrasion of continuous circulation. The only noticeable erosion was located around the periphery of the circulator rotor housing, on the rotor

nose-cone, and on the target of the mass flowmeter.

- (3) The sharp-edged orifice, an integral part of the mass flowmeter, was used to measure the gas mass flow rate independently of the solids flow rate. The close correspondence of the results obtained with the orifice and two gas flowmeter devices, a pitot tube and a thermistor probe, confirmed an earlier result that an orifice calibration for clean gas flow was also valid for gas flow containing small quantities of suspended solids.
- (4) A target-meter was used in conjunction with the sharp-edged orifice to determine the solids flow rate in the closed loop. The signal from strain gauges mounted on the target-meter support, after filtering and amplification, was found to be linear with the force of gas and solids striking the target. The response of the target-meter, determined during an open loop calibration, was dependent upon the size of the impacting particles. The size dependence was the result of the disturbed flow condition of the suspension upstream of the target.
- (5) Clean air friction factors were independent of the test section orientation and were in excellent agreement with accepted correlations.
- (6) Three types of (constant temperature) anemometer probes, thermistor, hot wire, and fiber-film, were used for meaningful measurements. All three probes followed the trends of King's law relating voltage and velocity. The thermistor was robust enough to withstand the impact of particles but was excessively sensitive to fluid temperature variations

and relatively insensitive to turbulence. The hot-wire was the most sensitive to turbulence of the three probe types but could not withstand the bombardment of particles. The fiber-film registered approximately 60 to 70 percent of the turbulence registered by the hot-wire, was only slightly temperature sensitive, and withstood the bombardment of particles for a considerable length of time. The fiber-film probe was, therefore, used to measure gas turbulence quantities in suspension flow. Probe contamination was accounted for in the mean velocity measurements by uniform translation of the fiber-film probe calibration curves.

- (8) The clean air velocity profiles confirmed the fully developed condition of the flow at the test sections. The agreement with the universal velocity profile was excellent. The profiles carried from the centerline of the tube to the outside edge of the buffer layer.
- (9) The clean air turbulence profiles indicated that the numerical results, although not the trends, of these measurements were strongly dependent on the type of probe and the overheat ratio. Measurements with the three probe types agreed with published results on the direction of increased turbulence intensity as probe locations and gas Reynolds numbers were varied. The turbulence intensity relative to the local mean velocity increased from the tube centerline, slowly at first, and then rapidly as the wall was approached. The turbulence intensity relative to the local mean velocity, at a fixed radial

position, decreased with increased values of Reynolds number. The measured magnitude of the turbulence intensities was highest for the hot-wire at its highest permissible overheat ratio and lowest for the thermistor probe. The results indicated that comparisons of numerical results obtained by different investigators are not valid unless identical measurement techniques are used.

- (10) The clean air turbulent spectra showed a tendency toward an energy variation with $k^{-5/3}$ at low wavenumbers and with k^{-7} at high wavenumbers. The spectrum analyses confirmed the indications of the intensity profile measurements that the fiber-film probe had relatively little sensitivity to small eddies.
- (11) The clean gas Nusselt numbers were within the range of accepted heat transfer correlations. The results were approximately the same for vertical and horizontal test sections and for the two gases, air and argon.
- (12) Large particles permitted circulation of higher loading ratios than smaller particle sizes. Larger Reynolds numbers also permitted circulation of higher loading ratios, although the Reynolds number effect was not as important or as consistent as the particle size effect.
- (13) Electrostatically induced deposition was important only for the #980 glass beads and the H-322 aluminum powder. Deposition induced by thermophoresis was observed on the cool walls of the water jacketed sections. The tendency toward deposition increased

with decreasing particle size because of agglomeration. Large individual particles did not show as strong a tendency to leave the main flow pattern. The aluminum powder, besides depositing heavily and being the most difficult particle to circulate, formed a rock hard buildup on any surface subject to direct bombardment.

- (14) Drag reduction was achieved for all five glass bead designations in the vertical test section. The maximum drag reduction was about 40% for both the #380 and #660 glass beads. The onset of drag reduction with loading ratio was progressively delayed with increasing particle size. The drag reduction was only a weak function of gas Reynolds number. The results for the #380 glass beads indicated that an optimum loading ratio existed for the maximum amount of drag reduction.
- (15) Drag reduction was achieved for the three smallest glass bead sizes in the horizontal test section. The results were almost identical to the results obtained in the vertical test section for the #980, #981, and #279 glass beads. The #380 and #660 glass bead suspensions yielded drag increases rather than drag reduction. Apparently gravity segregation and sedimentation had a negligible effect upon the drag reducing ability of the smaller particle sizes but had a detrimental effect upon that of the larger particle sizes.
- (16) The suspension air velocity profiles were similar to the corresponding clean air velocity profiles. The suspension profiles, when recast in terms of

the friction velocity and the universal velocity profile, indicated that drag reduction was caused by a thickening of the viscous sublayer and the buffer layer.

- (17) The suspension air turbulence intensity profiles all indicated that the presence of particles had the effect of increasing the air turbulence above the clean air values. The results, when normalized with friction velocity and plotted against the non-dimensional distance to the wall, enforced the conclusion that drag reduction in a gas-solids suspension could be characterized by a thickening of the viscous sublayer. There was no general turbulence suppression.
- (18) The turbulent spectra reflected the increased turbulence intensities measured in suspension flow with the appearance of higher spectral power densities at low wavenumbers. The questionable resolution of the probe and analyzer was apparent by the coincidence of the spectral power densities for both suspension and clean gas flows at high wavenumbers.
- (19) The particle velocity profiles were, within the experimental accuracy of the double flash photographic technique, the same as the air velocity profiles with the exception of a finite slip velocity at the wall. The results indicated that a continuum approach, predicting bulk slip velocities much larger than terminal settling velocities was invalid to describe the suspension profiles.
- (20) The glass-air suspension heat transfer measurements in the vertical test section indicated that the #279

and #380 glass beads were much more effective in reducing Nusselt numbers than in reducing friction factors. Reductions of up to 44% and 52% were achieved for the #279 and #380 glass beads respectively. The heat transfer measurements for suspensions of aluminum powder in argon indicated that particle thermal properties had no effect on Nusselt number reduction in the vertical test section. The similarity in behavior of the #380 (36.0 μ) glass beads and the H-322 (30.0 μ) aluminum powder suggested that the Nusselt number reduction in the vertical test section was a coincident result of the postulated drag reduction mechanism, that is, a thickening of the viscous sublayer.

- (21) Significant Nusselt number reduction, up to 38% and 44% for the #279 and #380 glass beads in air respectively, was also achieved for the horizontal test section. However, the horizontal test section Nusselt number results were not as consistent with the friction factor results as those in the vertical test section. Gravity sedimentation and thermophoresis causing deposition on the cool walls of the horizontal heat transfer test section complicated the results. The Reynolds analogy was not valid for horizontal flow because the particles did not just distort the structure of the turbulence, as they did in vertical flow, but actually became an active part of the heat transfer mechanism.
- (22) The drag reduction results could be predicted theoretically by calculating the effect of particles on the Townsend-Bakewell eddy structure in the

viscous sublayer. The particles have the effect of interfering with the equilibrium energy transfer between the Townsend-Bakewell eddies and the smaller energy dissipative eddies in the near wall region. Provided the particles do not produce excessive inertial effects, the particles can produce a thickened viscous sublayer and reduce the wall shear stress.

Appendix A

OPERATING INSTRUCTIONS

Operating instructions were prepared for two items of equipment that were not commercially purchased. The instructions included those needed for the warmup of the mass flowmeter and startup of the circulator. This appendix enumerates the operating instructions in detail.

MASS FLOWMETER WARMUP AND SHUTDOWN

The seven step warmup procedure for the mass flowmeter encompassed a two hour period. The steps are listed below:

- (1) Turn on the separate filter power supply.
- (2) Turn on the filter.
- (3) Plug in the preamplifier.
- (4) Turn on the power for the Brush recorder.
- (5) Set the recorder chart speed at the desired rate.
- (6) Set the sensitivity of the chart (controlled by the preamplifier) to the desired value.
- (7) After the pen trace has stabilized at a constant value, set the pen position to the desired location.

Shutdown involved reversing steps (1) through (7).

CIRCULATOR STARTUP AND SHUTDOWN

The circulator startup procedure could be initiated at any time; however, the actual operation of the circulator was delayed until the mass flowmeter had stabilized at no-flow conditions. The five step startup procedure is listed below:

- (1) Turn on the panel board transformer.
- (2) Throw the generator switch to "on".
- (3) Flip the circulator-motor switch to "on".
- (4) Select either the high speed or low speed range and turn the circulator on.
- (5) Regulate the speed using the "faster-slower" buttons and the panelboard tachometer. Change ranges by shutting down the circulator and selecting the other range button.

Shutdown was achieved by slowing the circulator speed to its minimum in the particular range, high or low, and reversing steps (1) through (3).

Appendix B

DATA SHEETS

Special data sheets were prepared to record data from flow measurements and from heat transfer measurements. This appendix shows an example of each data sheet.

Data sheets similar to the one shown in Figure 60 were used to record readings taken during all pressure drop, open loop calibration, and gas profile measurement runs. Additional information, for example weight and size designation of particles added to the loop, was also recorded on blank sections of the form.

Data sheets similar to the one shown in Figure 61 were used to record readings taken during all heat transfer measurements. The temperature numbering system corresponded to the thermocouple numbering system on the 24 point dial recorder.

```

E:
T AMBIENT ° F
TC(      μ/DIV)
MASS FLOWMETER      μV/DIV
P STATIC HORIZONTAL in Hg
ΔP ORIFICE in H2O
ΔP HORIZONTAL (S.F. 0. ) mm H2O
P STATIC VERTICAL in Hg
ΔP VERTICAL (S.F. 0. ) mm H2O
ΔP PITOT-STATIC (S.F. 0. ) mm H2O
% TACHOMETER
TRAVERSE LOCATION REV.
V-D.C. (volts)
V-RMS (volts)
SPECTRUM

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[illegible]

227

HEAT TRANSFER DATA SHEET

Date		Run Number - HT -	
Particle size number		Pump speed (%)	
Variac setting (volts)		Rotameter (G.P.M.)	
P _{static} (in Hg)		ΔP orifice (in H ₂ O)	
Strain gauge (mv)			
Wall temperatures (^o F)			
Lower vert.	T ₁ =	T ₂ =	
vert.	T ₃ =	T ₄ =	
vert.	T ₅ =	T ₆ =	
vert.	T ₇ =	T ₈ =	
vert.	T ₉ =	T ₁₀ =	
Upper vert.	T ₁₁ =	T ₁₂ =	
Bulk temperatures (^o F):			
Inlet vert.	T ₁₃ =	Outlet vert.	T ₁₄ =
Inlet TOPH	T ₁₅ =	Outlet TOPH	T ₁₆ =
Strain gauge	T ₁₇ =		
Inlet both	T ₁₈ =	Outlet both	T ₁₉ =
Pump temperatures (^o F):			
Top bearing	T ₂₀ =	Bot bearing	T ₂₁ =
Impellers	T ₂₂ =	T ₂₃ =	T ₂₄ =
Water temperatures (^o F):			
Vertical jacket top =		Vertical jacket bot =	
Horizontal top in =		Horizontal top out =	
Horizontal bot in =		Horizontal bot out =	
Weight added			

Figure 61

Heat Transfer Data Sheet

APPENDIX C

NOTES ON LENGTH AND TIME SCALES

Lumley (Sears and Van Dyke 1969:367-383) has described in a general manner the effects of the introduction of additional length and time scales into a turbulent flow. Although his analysis was part of a wider discussion of drag reduction in liquids by the addition of polymers in solution, the analysis has relevance to gas-solids drag reduction. Particles can introduce length scales such as the diameter and time scales such as the Stokesian relaxation time. The length and time scales for the particles used in this investigation are listed below:

<u>Particle Designation</u>	<u>Length Scale (Diameter)</u>	<u>Time Scale (Relaxation Time)</u>
#980	15.0 μ	0.00178 sec.
#981	21.0 μ	0.00348 sec.
#279	21.6 μ	0.00368 sec.
#380	36.0 μ	0.01022 sec.
#660	55.0 μ	0.0231 sec.
H-322	30.0 μ	0.00711 sec.

Lumley favored the use of reasoning based on time and length scales and interaction with the outer flow to describe drag reduction in liquid-polymer solutions. He suggested that drag reduction caused by the presence of suspended solids could also be attributed to a similar interaction. However,

he also felt that drag reduction by particles and fibers could be better described by the known interaction of the solids with the viscous sublayer (Lumley 1964). It is likely that both phenomena occur to some extent although sublayer interaction seems to dominate for gas-solids suspensions.

This appendix will describe in detail Lumley's qualitative analysis of the interaction of length and time scales with a turbulent flow. His analysis was based on two premises which he assumed would not be violated under drag reducing conditions. In fact both premises were corroborated by the results of the suspension air velocity profile measurements. First, he assumed that the local mean velocities in the drag reducing flow would scale, as in the original flow, with the friction velocity. This would be true if the slope of the logarithmic profile, although displaced away from the wall, remained the same. Indeed, the slope of the profile in the drag reducing suspension remained unchanged. Second, he assumed that the size of the energy containing eddies still scaled with y . The fact that the logarithmic law held confirmed the second assumption.

Lumley stated that y was representative of the scale of the energy containing eddies and that wavelengths of the same order as y were representative of the peak of the energy production range of the turbulent wavenumber spectrum.

Therefore:

$$ky = 1 \quad \text{or} \quad k^*y^* = 1$$

where

$$k^* = \frac{kv}{U_*} \quad \text{and} \quad y^* = \frac{yU_*}{\nu} \tag{95}$$

were used to represent the border of the production range of the spectrum and the equilibrium range. These regions and others described later are shown on Figure 62.

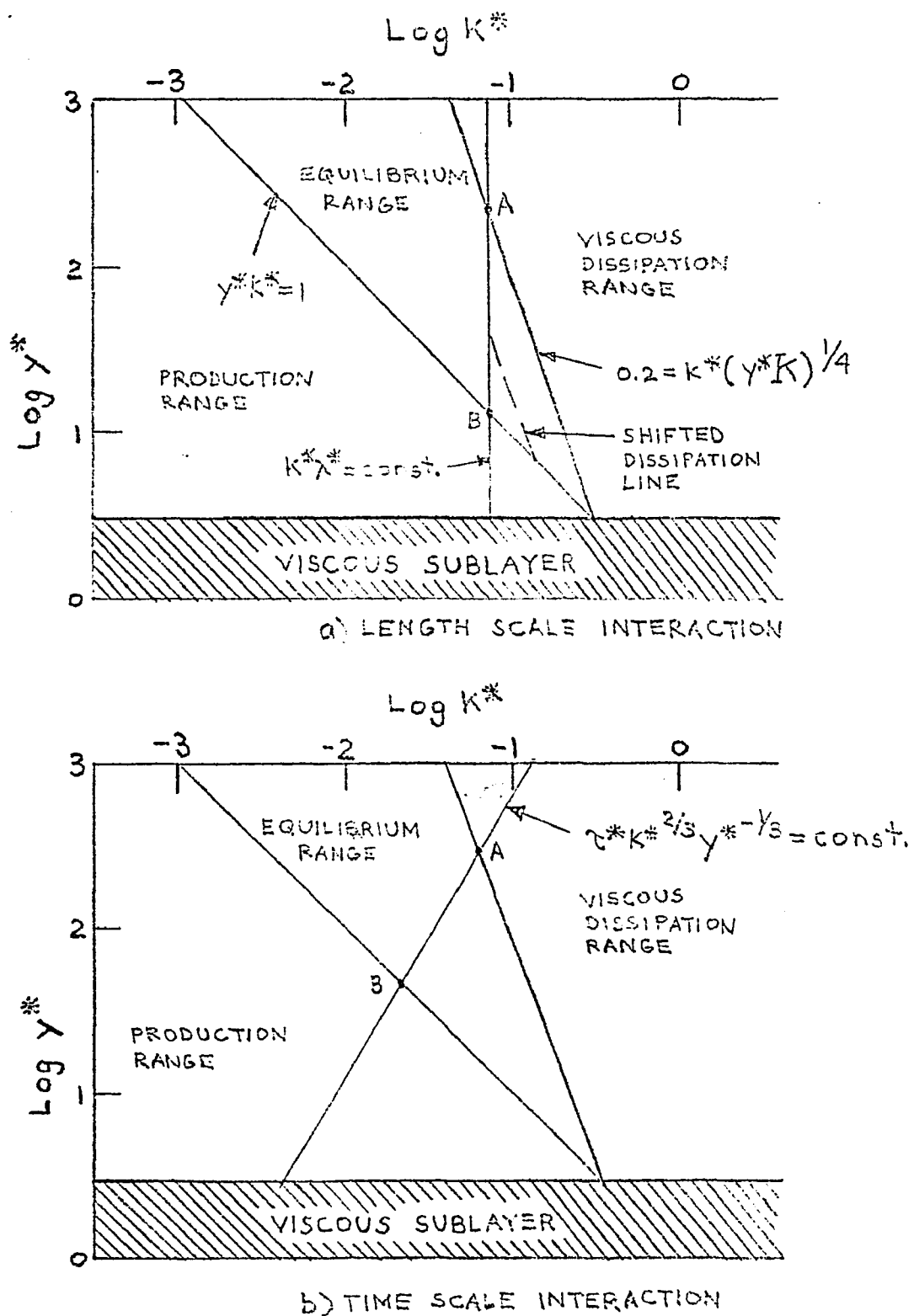


Figure 62
Scaling Relationships

The peak of the dissipation range of the wavenumber spectrum has been shown to occur at $k\eta = 0.2$ (Hinze 1959:184), where η is the equilibrium range length scale. η can be shown to be:

$$\eta = (\nu^3 / \epsilon)^{1/4} \quad (96)$$

from dimensional reasoning (Hinze 1959:184). ϵ is the turbulent production per unit mass which can be expressed as:

$$\epsilon = U_*^4 / K y^* \quad (97)$$

where K is the Von Karman constant.

Using the scaling factors U_* and y^* , the peak of the dissipation range can be expressed as:

$$k^*\eta^* = 0.2 \quad \text{where} \quad \eta^* = (y^*K)^{1/4} \quad (98)$$

In terms of the coordinates of Figure 62, the peak of the dissipation range can be described by:

$$0.2 = k^*(y^*K)^{1/4} \quad (99)$$

The area between the two lines represented by equations 95 and 99 describes the equilibrium range of turbulence in pipe flow. The intercept of equations 95 and 99 at $y^* = 6.35$ locates the position where production and dissipation become equal. This location corresponds roughly to the edge of the viscous sublayer.

Referring to Figure 62a, consider the effect of introducing a length scale λ into the flow. The ratio of the new length scale to the length scale of a fixed eddy size is given by:

$$k\lambda = \text{const.} \quad \text{or} \quad k^*\lambda^* = \text{const.} \quad (100)$$

where

$$\lambda^* = \frac{\lambda U_*}{\nu} .$$

Assuming a fixed constant, equation 100 appears as a vertical line in Figure 62a. As the wall shear, indicated by U_* , increases, equation 100 requires that λ^* increase and k^* decrease. The vertical line, therefore, will shift to the left. On the other hand, if the wall shear is very low, the line will shift far to the right and move completely out of the equilibrium range. The region, to the right of the equilibrium range is dominated by viscosity. The flow in this range is essentially non-turbulent and cannot be affected by changes in turbulent scales. Therefore, the introduction of a length scale combined with U_* and k^* such that its interaction regime is to the right of the equilibrium range will have no effect upon the turbulence. Physically this means that the ratio of λ to the smallest length scale of the turbulence, that is λ to ν/U_* , is itself very small. This may explain why drag reduction in liquid polymer solutions is often not seen or negligible unless the shear stress, as indicated by the friction velocity, is above some minimum value. (Davies 1972:285-291). On the other hand, no such limitation has been determined for drag reduction in gas-solids suspensions indicating that the length scale for the particles is much larger than the length scale for polymer molecules.

Consider the interaction of the length scale λ with the turbulence if the vertical line falls within the equilibrium range. Above A the turbulence is unaffected (viscosity

dominates), between A and B the dissipative eddy scales are affected, and below B the energy containing eddies (and the momentum transfer in terms of Reynolds stress) are affected.

In order to produce a thickened sublayer, the dissipation line would have to shift to the left and intercept the production line further from the wall as shown in Figure 62a. Since the Von Karman constant K has been shown to remain unchanged, the dissipation line could be shifted only by a deviation from the constant 0.2 in equation 98. The sublayer thickening will be limited to the value of y^* corresponding to point B since only those eddies to the right of the vertical line are affected.

The same reasoning, as applied to length scales, can be applied to time scales. Consider the introduction of a time scale τ . The time scale of a turbulent eddy can be expressed as $(k^2 \epsilon)^{-1/3}$. (The eddy time scale can be derived by taking its length scale $1/k$ as the equivalent of the length scale of equation 96 and by combining it with the Kolmogorov velocity scale, $(\nu \epsilon)^{1/4}$ (Hinze 1959:184).) The ratio of a fixed eddy time scale to τ is given by:

$$\tau (k^2 \epsilon)^{1/3} = \text{const.}$$

or

$$\tau * k^{2/3} y^{*-1/3} = \text{const.} \quad (101)$$

This line is drawn on Figure 62b. Again above point A, the turbulence is unaffected. Between A and B the dissipative eddy scales are affected, and below B the energy containing eddies are affected. The same reasoning as used for the length scales can be invoked to show that the viscous sublayer can thicken by the introduction of the proper time scales and by a leftward shift of the dissipation line.

LITERATURE CITED

- Abel, W.T., Bluman, D.E., and O'Leary, J.P. 1963. Gas-solids suspensions as heat-carrying mediums, ASME Paper 63-WA-210.
- Babcock and Wilcox Co. 1960a. Gas-suspension task III final report, BAW-1201.
- Babcock and Wilcox Co. 1960b. Supplement to the gas-suspension task III final report, BAW-1207.
- Bakewell, H.P. and Lumley, J.L. 1967. Viscous sublayer and adjacent wall region in turbulent pipe flow. *Physics of Fluids*, 10, 1880-1889.
- Batchelor, G.K. 1967. An Introduction to Fluid Dynamics, Cambridge: Cambridge University Press.
- Bluman, D.E., Galli, A.F., Coates, N.H., Spencer, J.D., and Rosenecker, C.N. 1967. Heat transfer coefficients for continuously recirculated gas-solids suspensions. BuMines Rept. of Inv. 7057.
- Boothroyd, R.G. 1966. Pressure drop in duct flow of gaseous suspensions of fine particles. *Trans. Instn. Chem. Engrs.*, 44, T306-T313.
- Boothroyd, R.G. 1967. Turbulence characteristics of the gaseous phase in duct flow of a suspension of fine particles. *Trans. Instn. Chem. Engrs.*, 45, T297-T310.
- Boothroyd, R.G. 1971. Flowing Gas-Solids Suspensions. London: Chapman and Hall Ltd.
- Boothroyd, R.G. and Haque, H. 1970. Fully developed heat transfer to a gaseous suspension of particles flowing turbulently in ducts of different size. *J. Mech. Eng. Sci.*, 12, 191-200.

- Boyce, M.P. and Blick, E.F. 1970. Fluid flow phenomena in dusty air. Trans. ASME, J. Basic Engrg., 92, 495-502.
- Chand, P., 1970. Viscosity of dusty gases. Chem. Eng. Sci., 25, 1019-1024.
- Clark, R.H., Charles, D.E., Richardson, J.F., and Newitt, D.M. 1952. Pneumatic conveying, Part I: The pressure drop during horizontal conveyance. Trans. Instn. Chem. Engrs., 30, 209-224.
- Corino, E.R. and Brodkey, R.S. 1969. A visual investigation of the wall region in turbulent flow. West Va. Univ.-Kanawha Valley Grad Ctr. Professional Development Lecture Vol. 1.
- Davies, J.T. 1972. Turbulence Phenomena An Introduction to the Eddy Transfer of Momentum, Mass, and Heat, Particularly at Interfaces. New York: Academic Press.
- Depew, C.A. 1960. Heat transfer to flowing gas-solids mixtures in a vertical circular duct. UCRL 9280, Lawrence Radiation Laboratory, U. of Cal., Berkeley, Calif., July 11, 1960.
- Depew, C.A. and Cramer, E.R. 1970. Heat transfer to horizontal gas-solid suspension flows. Trans. ASME, J. Heat Transfer, 92, 77-82.
- Depew, C.A. and Farbar, L. 1963. Heat transfer to pneumatically conveyed glass particles of fixed size. Trans. ASME, J. Heat Transfer, 85, 164-172.
- Doig, I.D. and Roper, G.H. 1967. Air velocity profiles in the presence of concurrently transported particles. Ind. Eng. Chem. Fundamentals, 6, 247-256.
- Dwyer, H.A. and Peskin, R.L. 1965. A study of the mechanics of turbulent gas-solid shear flows. ASME Paper 65-WA/FE-24.
- Farbar, L. and Depew, C.A. 1963. Heat transfer effects to gas-solids mixtures using solid spherical particles of uniform size. Ind. Eng. Chem. Fundamentals, 2, 130-135.

- Farbar, L. and Morley, M.J. 1957. Heat transfer in flowing gas-solids mixtures in a circular tube. Ind. Eng. Chem., 49, 1143-1150.
- Garrett, T. 1964. Heat transfer characteristics of dilute phase solids-air suspensions in turbulent pipe flow. Ph.D. Thesis, Dep. of Engg., Cambridge University, England.
- Gibson, H.G., Abel, W.T., and Fasching, G.E. 1963. Meter for determining mass flow of solids in multi-phase fluids. Proc. Multi-Phase Flow Symp., ASME, Philadelphia, Pa., Nov. 17-22, 1963, 49-54.
- Halstrom, E.A.N. 1953. Design of experimental apparatus for the study of two-phase flow in circular straight pipe. M.S. Thesis, Princeton U., Princeton, N.J.
- Haque, H. and Boothroyd, R.G. 1969. Heat and momentum transfer analogy in dense aerosols flowing turbulently in ducts. Powder Technol., 2, 305-307.
- Hariu, O.H. and Molstad, M.C. 1949. Pressure drop in vertical tubes in transport of solids by gases. Ind. Eng. Chem., 41, 1148-1160.
- Hawes, R.E., Holland, E., Kirby, G.T., and Weller, R. 1964. An experimental investigation into heat transfer and pressure drop properties of gaseous suspensions of solids. Atomic Energy Est. Report AEEW-R244.
- Hinze, J.O. 1959. Turbulence An Introduction to Its Mechanism and Theory. New York: McGraw-Hill Book Co.
- Jepson, G., Poll, A., and Smith, W. 1963. Heat transfer from gas to wall in a gas/solids transport line. Trans. Instn. Chem. Engrs., 41, 207-211.
- Jotaki, T. and Tomita, Y. 1971. Turbulent friction drag of a dusty gas. Paper C5, Proc. 1st Int. Conf. on Pneumatic Transport of Solids in Pipes, BHRA Fluid Engng., Cranfield, U.K. (September 1971).
- Kline, S.J., Reynolds, W.C., Schraub, F.A., and Runstadler, P.W. 1967. The structure of turbulent boundary layers. J. Fluid Mech., 30, 741-773.

- Kramer, T.J. and Depew, C.A. 1972a. Experimentally determined mean flow characteristics of gas-solid suspensions. ASME Paper 72-FE-29.
- Kramer, T.J. and Depew, C.A. 1972b. Analysis of mean flow characteristics of gas-solids suspensions. ASME Paper 72-FE-30.
- Kreith, F. 1964. Principles of Heat Transfer. Scranton: International Textbook Co.
- Laufer, J. 1954. The structure of turbulence in fully developed pipe flow. NACA Tech. Rept. 1174.
- Little, R.C. 1969. A review of 6.1 work units in drag reduction with emphasis on current problems, progress, and landmarks. NRL Memorandum Report 1957, Naval Research Laboratory, Washington.
- Liu, J.T.C. 1965. On the hydrodynamic stability of parallel dusty gas flows. *Physics of Fluids*, 8, 1939-1945.
- Lumley, J.L. 1964. The reduction of skin friction drag. Fifth Symposium on Naval Hydrodynamics, Bergen, Norway, 915-946.
- Mason, J.S. and Boothroyd, R.G. 1971. Comparison of friction factors in pneumatically conveyed suspensions using different-sized particles in pipes of varying sizes. Paper C1, Proc. 1st Int. Conf. on Pneumatic Transport of Solids in Pipes, BHRA Fluid Engng. Cranfield, U.K. (September 1971).
- McCarthy, H.E. and Olson, J.H. 1968. Turbulent flow of gas-solids suspensions. *Ind. Eng. Chem. Fundamentals*, 7, 471-483.
- Michael, D.H. 1964. The stability of plane Poiseuille flow of a dusty gas. *J. Fluid Mech.*, 18, 19-32.
- Michael, D.H. 1965. Kelvin-Helmholtz instability of a dusty gas. *Proc. Camb. Phil. Soc.*, 61, 569-571.
- Monin, A.S. and Yaglom, A.M. 1971. Statistical Fluid Mechanics: Mechanics of Turbulence. Cambridge: MIT Press.

- Owen, P.R. 1969. Pneumatic transport, J. Fluid Mech., 39, 407-432.
- Perry, J.H. (editor) 1963. Chemical Engineers' Handbook, Fourth Edition, New York: McGraw-Hill Book Co.
- Peskin, R.L. and Dwyer, H.A. 1964. A study of the mean flow characteristics of gas-solid suspensions. Technical Report No. 101-ME-F, Dept. of Mech. Engng., Rutgers University.
- Peskin, R.L. and Rin, C.H. 1964. A study of the effect of the presence of suspended solids on LaGrangian properties of fluid turbulence. Technical Report No. 102-ME-F, Dept. of Mech. Engng., Rutgers University.
- Peskin, R.L. and Wallace, J.P. 1966. Some statistical turbulence properties of the fluid phase in a gas-solid suspension. Technical Report No. 112-ME-F, Dept. of Mech. Engng., Rutgers University.
- Peters, L.K., Bender, D.W., and Klinzing, G.E. 1972. Friction modification in a coupled system. Paper 20f, AIChE Annual Meeting.
- Pfeffer, R. and Rossetti, S.J. 1971. Experimental determination of pressure drop and flow characteristics of dilute gas-solid suspensions. NASA CR-1894.
- Pfeffer, R., Rossetti, S., and Lieblein, S. 1966. Analysis and correlation of heat-transfer coefficient and friction factor data for dilute gas-solid suspensions. NASA TN D-3603.
- Rasmussen, C.G. 1966. Measurement of turbulence characteristics. DISA Information No. 3.
- Reddy, K.V.S. and Pei, D.C.T. 1969. Particle dynamics in solids-gas flow in a vertical pipe. Ind. Eng. Chem. Fundamentals, 8, 400-497.
- Reddy, K.V.S., Van Wijk, M.C., and Pei, D.C.T. 1967. Stereophotogrammetry in particle-flow investigations. Unpublished Report, Dept. of Chem. Engng., University of Waterloo, Ontario.

- Richardson, J.J. and McLeman, M. 1960. Pneumatic conveying -- Part II Solids velocities and pressure gradients in a one-inch horizontal pipe. Trans. Instn. Chem. Engrs., 38, 257-266.
- Rodriguez, J.M., Patterson, G.K., and Zakin, J.L. 1969. Turbulence measurements in drag reducing flow. Unpublished Report, University of Missouri-Rolla.
- Rosenecker, C.N., Coates, N.H., and Lucas, H.G. 1966. Power required to circulate gas-solids suspensions. BuMines Report of Inv. 7019.
- Rossetti, S.J. 1969. Experimental determination of pressure drop and flow characteristics of dilute gas-solid suspensions. Ph.D. Thesis, City University of New York.
- Rossetti, S.J. and Pfeffer, R. 1972. Drag reduction in dilute flowing gas-solid suspensions. AIChE J. 18, 31-39.
- Saffman, P.G. 1962a. On the stability of laminar flow of a dusty gas. J. Fluid Mech., 13, 120-128.
- Saffman, P.G. 1962b. Flow of a dusty gas between rotating cylinders. Nature, 193, 463-464.
- Savins, J.G. and Virk, P.S. (coeditors) 1971. Drag Reduction. Chemical Engineering Progress Symposium Series Volume 67. New York: American Institute of Chemical Engineers.
- Schlichting, H. 1968. Boundary Layer Theory, Sixth Edition. New York: McGraw-Hill Book Co.
- Schluderberg, D.C., Whitelaw, R.L., and Carlson, R.W. 1961. Gaseous suspensions--a new reactor coolant. Nucleonics, 8, 67-73.
- Sears, W.R. and Van Dyke, M. (coeditors) 1969. Annual Review of Fluid Mechanics, Volume 1. Palo Alto: Annual Reviews, Inc.
- Segler, G. 1951. Pneumatic Grain Conveying. (Silsoe, Beds.: National Institute of Agricultural Engineering).
- Soo, S.L. 1967. Fluid Dynamics of Multiphase Systems. Waltham: Blaisdell Publishing Co.

- Soo, S.L. and Trezek, G.J. 1966. Turbulent pipe flow of magnesia particles in air. *Ind. Eng. Chem. Fundamentals*, 5, 388-392.
- Soo, S.L., Trezek, G.J. Dimick, R.C., and Hohnstreiter, G.F. 1964. Concentrations and mass flow distributions in a gas-solid suspension. *Ind. Eng. Chem. Fundamentals*, 2, 98-106.
- Soo, S.L. and Tung, S.K. 1971. Pipe flow of suspensions in turbulent fluid. Electrostatic and gravity effects. *Appl. Sci. Res.*, 24, 83-97.
- Sproull, W.T. 1958. Viscosity of dusty gases. *Bull. Amer. Phys. Soc.*, II, 3, 411.
- Sproull, W.T. 1960. Viscosity of dusty gases. *Bull. Amer. Phys. Soc.*, II, 5, 497.
- Sproull, W.T. 1961. Viscosity of dusty gases. *Nature*, 190, 976-978.
- Sproull, W.T. 1962. Flow of a dusty gas between rotating cylinders. *Nature*, 193, 464.
- Streeter, V.L. 1962. *Fluid Mechanics*, Third Edition. New York: McGraw-Hill Book Co.
- Sukomel, A.S., Tsvetkov, F.F., and Kerimov, R.V. 1967. A study of local heat transfer coefficients from a tube wall to a turbulent flow of gas bearing suspended solid particles. *Teploenergetika*, 14, 77-80. *App. Mech. Rev.* 1968, 21, 6834.
- Szekely, J. and Carr, R. 1966. Heat transfer in a cyclone. *Chem. Eng. Sci.*, 21, 1119-1132.
- Tien, C.L. and Quan, V. 1962. Local heat transfer characteristics of air-glass and air-lead mixtures in turbulent pipe flow. ASME Paper 62-HT-15.

- Tills, J.W. 1971. An introduction to pneumatic conveying with particular reference to granular sugar. Paper A6, Proc. 1st Int. Conf. on Pneumatic Transport of Solids in Pipes, BHRA Fluid Engng., Cranfield, U.K. (September 1971).
- Townes, H.W., Gow, J.L., Powe, R.E., and Weber, N. 1971. Turbulent flow in smooth and rough pipes. ASME Paper 71-WA/FE-7.
- Townsend, A.A. 1966. The Structure of Turbulent Shear Flow. Cambridge University Press, Cambridge.
- Wachtell, G.P. and Waggener, J.P. 1964. Flow stability of gas-solids suspensions. The Franklin Institute Report F-B1938.
- Wen, C.Y. and Simons, H.P. 1959. Flow characteristics in horizontal fluidized solids transport. AIChEJ, 5, 263-267.
- Wilkinson, G.T. and Norman, J.R. 1967. Heat transfer to a suspension of solids in a gas. Trans. Instn. Chem. Engrs., 45, T314-T318.

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