



ALMA MATER STUDIORUM
UNIVERSITÀ DI BOLOGNA

DOTTORATO DI RICERCA IN
SCIENZE E TECNOLOGIE AEROSPAZIALI

Ciclo 38

Settore Concorsuale: 09/A1 - INGEGNERIA AERONAUTICA, AEROSPAZIALE E NAVALE

Settore Scientifico Disciplinare: ING-IND/05 - IMPIANTI E SISTEMI AEROSPAZIALI

NOVEL TESTBED ARCHITECTURES FOR SMALL SATELLITES ATTITUDE
CONTROL AND POINTING STABILISATION SYSTEMS

Presentata da: Andrea Curatolo

Coordinatore Dottorato

Dario Modenini

Supervisore

Paolo Tortora

Co-supervisore

Dario Modenini

Esame finale anno 2026

Abstract

The space sector has undergone a paradigm shift over the last two decades, from large and costly satellites developed by a few nations and major companies to small satellites developed by a wide range of actors. This new approach, often referred to as *newspace*, is planned to play an increasingly important role in the upcoming years. CubeSats are an example of these new small satellites and enable low-cost missions for a wide range of fields. Their performance and reliability are however limited, with many missions experiencing in-flight issues or failure. Among other factors, the teams' lack of experience and the oversight of functional system level testing have been identified as the leading causes of the low success rate. The Attitude Determination and Control System is one of the critical subsystems on board a satellite, on which the mission's success depends. It is, however, a subsystem difficult to test "like you fly" as it requires free rotational motion in a low torque environment with sensor stimulus. Attitude testing facilities for CubeSats exist but are limited in terms of simulation capabilities, lack flexibility in hosting different CubeSat sizes and present disturbance torque levels orders of magnitude higher than those in space. To address this gap, a new facility has been developed for the European Space Agency (ESA) and has been installed at the European Space Research and Technology Centre (ESTEC). The new facility can be used to provide a verification & validation tool that can truly support a 'test-as-you-fly' philosophy. The development of part of this facility is described in this thesis.

Just as reliability remains a challenge for small satellites, their performance is also limited compared to that of traditional large spacecraft, even though attitude control and payload pointing requirements are becoming increasingly demanding. Many modern missions require spacecraft to maintain fine pointing over a long time. This, in turn, demands an ultra-quiet environment, yet on-board actuators such as reaction wheels generate microvibrations that can be amplified by the lightweight structures of small satellites. An innovative approach to enable fine pointing consists in employing a platform that measures and actively reduces microvibrations. To this end, the ESA GNC, AOCS & Point-

ing Laboratory has developed a high-performance stabilisation breadboard used to validate and assess the pointing stability of future space missions in the presence of microvibrations. The new configuration of this breadboard requires validation, which involves system identification, controller design and experimental testing.

This thesis addresses both challenges through two main lines of contribution. The first concerns the CubeSat attitude testing facility, where an automatic mass balancing system was designed, assembled and validated. The balancing system enables a low torque environment for the testing of a wide range of CubeSats sizes. During balancing, the test platform's centre of mass is dynamically shifted and made to coincide with its centre of rotation. The residual torque level achieved after the balancing procedure is consistent with the state of the art of these types of facilities. The integration of the balancing system within the facility makes it distinctive in CubeSat size hosting capabilities, sensor stimulus capabilities and residual torque levels. In addition, a wireless communication module implementing different space communication protocols was developed to provide the facility with a wireless interface for communication with the device under test.

The second line of contribution consists of the experimental validation of the new configuration of the ESA high-performance stabilisation breadboard, encompassing system identification, controller synthesis and automated experimental verification. Experimental results confirmed that the new breadboard configuration is an effective tool for mitigating microvibrations and enabling fine pointing in future small satellite missions.

Taken together, the two lines of research provide a solid contribution towards enhancing the reliability and performance of small satellites' pointing systems.

Acknowledgments

I would like to express my sincere gratitude to Paolo Tortora and Dario Modenini for their guidance over the years. I could not have asked for better mentors. True leaders like you are the ones who help people grow and improve.

My thanks also go to my colleagues in the Microsat team, it has been a pleasure to work with you. A special mention to the members of the Tecnopolo; thanks for all the great lunches we shared.

I would like to thank Fabrice Boquet for his supervision during my visiting period at ESTEC. Your kindness and expert guidance were essential for me to get the most out of my time there. A big thanks also to the other members of TEC-EC; you made me feel truly welcome in the division.

I am so grateful to the friends whose company was so precious during these years: the ones from Pizzo and Forlì, the ones from Leiden and ESTEC, and the many others met along the way. We've made great memories together, and there are many more to come.

Finally, a huge thank you to my family. The environment in which a person grows has a profound impact on one's life. I was lucky enough to be supported in every possible way while being given the freedom to create my own path.

Contents

Abstract	iii
Acknowledgements	v
Contents	vi
List of Tables	xi
List of Figures	xiii
List of Acronyms	xxii
Introduction	xxix
Part I	1
1 Part I, Introduction	3
1.1 CubeSat characteristics and technology	6
1.2 CubeSat development, reliability and testing	11
1.3 Review of CubeSat attitude testing facilities	13
1.4 Highlight of existing gap in CubeSat ADCS testing facilities	24
2 Preliminaries	27
2.1 Attitude dynamics and kinematics	27
2.1.1 Frames of reference	27
2.1.2 Attitude representation	30
2.1.3 Attitude kinematics	32
2.1.4 Attitude dynamics	33
2.2 The environmental disturbance torques	33
2.2.1 Gravity gradient torque	34
2.2.2 Magnetic torque	34
2.2.3 Aerodynamic torque	35

2.2.4	Solar radiation pressure torque	36
2.2.5	Disturbance torque for a CubeSat in Low Earth Orbit . . .	36
2.3	Model of a rotating platform with integrated balancing system . .	39
2.4	Space communication protocols	42
2.4.1	The Open System Interconnection model	43
2.4.2	The Cubesat Space Protocol	45
2.4.3	The Space Packet Protocol	46
3	Facility design	49
3.1	Facility requirements	50
3.2	Facility architecture	55
3.3	Facility subsystems	59
3.3.1	Magnetic simulator	59
3.3.2	Sun simulator	61
3.3.3	Albedo lamp	62
3.3.4	Ground-truth system and star tracker emulator	63
3.3.5	Air bearing	65
3.3.6	Air bearing table and CubeSat mounting	67
3.4	Mass balancing	70
3.4.1	Coarse balancing	70
3.4.2	Fine mass balancing	78
3.5	Communication module	98
4	Results of facility commissioning and performance	103
4.1	Mass balancing	103
4.1.1	Estimation of angular velocity from the gravity vector . . .	104
4.1.2	Results of coarse mass balancing	108
4.1.3	Results of fine mass balancing	114
4.2	Summary of mass balancing results	127
4.3	Communication module test results	130
4.3.1	Absolute clock synchronisation test	131
4.3.2	Path delay estimation	131
4.3.3	Quality of transmission evaluation	133
5	Part I, Conclusions	137

Part II	140
6 Part II, Introduction	143
7 Preliminaries	147
7.1 Robust control	147
7.1.1 Linear systems	148
7.1.2 Nominal stability and performance	149
7.1.3 Interconnected system	152
7.1.4 Structured singular value, robust stability and performance	153
7.1.5 Robust control synthesis technique and tools	155
8 Methods	157
8.1 The ESA high-performance stabilisation breadboard	157
8.2 Presentation of data in non-dimensional form	158
8.3 System identification	159
8.3.1 Non-parametric identification	159
8.3.2 Parametric identification	161
8.3.3 Uncertainty modelling	162
8.4 Controller synthesis	164
8.4.1 LTI controllers design using robust control techniques . . .	166
8.4.2 LPV controllers design using robust control techniques . .	168
9 Results	173
9.1 System identification	173
9.1.1 Non-parametric identification	173
9.1.2 Parametric identification	181
9.1.3 Uncertainty modelling	188
9.1.4 Summary of system identification results	190
9.2 Controllers synthesis and testing	191
9.2.1 SISO LTI control	191
9.2.2 MIMO LTI control	200
9.2.3 SISO LPV control	210
9.2.4 MIMO LPV control	214
9.2.5 Summary of controller synthesis and testing results	218

9.3 Graphical user interface for system identification and controller synthesis	220
10 Part II, Conclusions	225
11 Conclusions	227
11.1 Part I	227
11.2 Part II	228
11.3 Ideas for future development	229
Appendices	231
A Coarse mass balancing calculations	233

List of Tables

1.1	CubeSat mass, size and allowed CM to GC distance	9
1.2	Existing CubeSat ADCS testing facilities. (Sim. = Simulation, Emu. = Emulation, Mag. = Magnetic)	21
2.1	Orbital characteristics for the reference LEO mission	37
2.2	CubeSat mass, sizes and principal moments of inertia	37
2.3	Environmental disturbance torque calculations for different sizes of CubeSats	38
3.1	Facility requirements	50
3.2	Coarse mass balancing sizing scenarios (H = Horizontal, V = Ver- tical, SI = SpaceInventor)	74
3.3	Set of counterweights	77
3.4	Xsens IMU attitude estimation performance	79
3.5	Xsens IMU gyroscope performance	80
3.6	Linear actuators technical specifications	80
4.1	Comparison of estimation of angular velocity projection from gy- roscope and from gravity vector, gravity vector RMS values	105
4.2	Comparison of estimation of angular velocity projection from gy- roscope and from gravity vector, stepper motor position RMS values	106
4.3	Fine mass balancing steps and results, 1U CubeSat	114
4.4	Fine mass balancing steps and results, 3U CubeSat	117
4.5	Fine mass balancing steps and results, 6U CubeSat	120
4.6	Fine mass balancing steps and results, 12U CubeSat	123
4.7	Comparison of in-orbit torque with CubeDyna facility torque . . .	128
9.1	SISO LTI controller design performance weights parameters	192
9.2	SISO LTI controller design performance metrics	193
9.3	Results of simulation and test on breadboard of the SISO LTI con- trollers	197

9.4	MIMO LTI controller design performance weights parameters . . .	200
9.5	MIMO LTI controller design performance metrics	201
9.6	Results of simulation and test on breadboard for the MIMO LTI controllers	206
9.7	SISO LPV controller design performance weights parameters . . .	210
9.8	SISO LPV controller design performance metrics	210
9.9	Results of simulation and test on breadboard of the SISO LPV con- troller	211
9.10	MIMO LPV controller design performance weights parameters . .	214
9.11	MIMO LPV controller design performance metrics	214
9.12	Results of simulation for the MIMO LPV controller	215
9.13	Results of controllers testing on the breadboard (SISO LTI, MIMO LTI and SISO LPV) and in simulation (MIMO LPV)	219

List of Figures

1.1	Nanosatellites launched by type, 1998 – 2025 (Kulu, 2025)	5
1.2	CubeSat sizes listed in the CubeSat design specification (SLO, 2025)	7
1.3	CubeSat coordinate axes definition in the CubeSat design specification (SLO, 2025)	8
1.4	Different types of ABT: Tabletop (a), Umbrella (b), Dumbbell (c), Hollow (d)	15
1.5	Attitude testing facility residual torque level at varying CubeSat size. The bubble size represents the number of sensor stimulus, with 1 being only the air bearing.	24
2.1	Laboratory frame definition	29
2.2	Air bearing table platform frame definition for the 1U platform (left) and for the 3U-12U platform (right)	30
2.3	Mass balancing reference frames and CM position vector	39
2.4	The Cubesat Space Protocol header (libcsp authors, 2023a)	46
2.5	The SPP context within the CCSDS Protocol Stack (CCSDS, 2020)	47
3.1	CubeDynA product tree	57
3.2	CubeDynA architectural diagram	58
3.3	Magnetic subsystem connections	60
3.4	The CubeDynA Sun simulator	61
3.5	CubeDynA albedo lamp (without neutral light attenuators)	62
3.6	CubeDynA ground-truth system	63
3.7	Air bearing module on the pedestal and air bearing table	65
3.8	Compressor and air conditioning unit	66
3.9	The air bearing table platform with on top different CubeSats mock-ups and relative counterweights. Top-left: 1U CubeSat; top-right: 3U CubeSat; bottom-left: 6U CubeSat, bottom-right: 12U CubeSat	68
3.10	The CubeDynA MGSE, holding 12U mock-up: left, holding ABT and 6U mock-up: right	69

3.11 CubeDynA coarse mass balancing: threaded pins and mass balancing disk	72
3.12 Screenshot of coarse mass balancing spreadsheet, 1U platform . .	73
3.13 Screenshot of coarse mass balancing spreadsheet, 3U-12U platform	73
3.14 Spacers used to shift up the CM position. The picture on the left shows the spacers on the air bearing module, the picture on the right shows the spacers on the 1U platform	74
3.15 Mass balancing disks and threaded pins used to extend the distance between ABT and counterweights	76
3.16 Coarse balancing decision diagram	76
3.17 High level architectural diagram of fine mass balancing system . .	79
3.18 The 1U and 3U-12U platforms with highlighted the fine mass balancing system components	81
3.19 Stepper motor mass mounted on the stepper motor carrier of the 1U platform (left) and of the 3U-12U platform (right)	82
3.20 The accuracy of in-plane balancing depends on the vertical CM to CR offset. In case 2 the accuracy is improved with respect to case 1	86
3.21 Fine mass balancing algorithm	90
3.22 State machine flowchart for connection, initialization and reset of the on-board balancing software	91
3.23 State machine flowchart for the status update, gyroscopes calibration, gain updates, stepper motor position update functions of the on-board balancing software	91
3.24 State machine flowchart for the balancing function of the on-board balancing software	91
3.25 CubeDynA mass balancing GUI	92
3.26 The ADCS mock-up of the heritage air bearing facility of the University of Bologna (Curatolo et al., 2022). See text for label explanation.	94
3.27 Comparison of the projection in the x-y plane of the normalized gravity acceleration in the case of classic balancing (solid line) and mixed balancing (dashed line) (Curatolo et al., 2022).	96
3.28 Comparison of the sliding masses' position in the case of classic balancing (solid line) and mixed balancing (dashed line) (Curatolo et al., 2022).	97

3.29 Magnetorquers dipole during mixed balancing (Curatolo et al., 2022).	97
3.30 CubeDynA communication module: Raspberry Pi Zero W with CAN HAT	100
3.31 Schematics of the communication module functionalities	101
4.1 Comparison of estimation of angular velocity projection from gyroscope and from gravity vector, x axis	105
4.2 Comparison of estimation of angular velocity projection from gyroscope and from gravity vector, y axis	106
4.3 Comparison of stepper motor position for the two different estimation method of projected angular velocity, x axis	107
4.4 Comparison of stepper motor position for the two different estimation method of projected angular velocity, y axis	107
4.5 Results of coarse mass balancing for the 1U CubeSat with additional GSE	109
4.6 Results of coarse mass balancing for the 1U CubeSat with increased mass to simulate additional GSE contribution	109
4.7 1U CubeSat mock-up on the testing platform	109
4.8 Results of coarse mass balancing for the 3U CubeSat	110
4.9 3U CubeSat mock-up on the testing platform	111
4.10 Results of coarse mass balancing for the 6U CubeSat	112
4.11 6U CubeSat mock-up on the testing platform	112
4.12 Results of coarse mass balancing for the 12U CubeSat	113
4.13 12U CubeSat mock-up on the testing platform	113
4.14 In-plane balancing step, 1U CubeSat	116
4.15 Inertia and vertical offset estimation step, 1U CubeSat	116
4.16 Disturbance torque estimation step, 1U CubeSat	117
4.17 In-plane balancing step, 3U CubeSat	119
4.18 Inertia and vertical offset estimation step, 3U CubeSat	119
4.19 Disturbance torque estimation step, 3U CubeSat	120
4.20 In-plane balancing step, 6U CubeSat	122
4.21 Inertia and vertical offset estimation step, 6U CubeSat	122
4.22 Disturbance torque estimation step, 6U CubeSat	123
4.23 In-plane balancing step, 12U CubeSat	125

4.24	Inertia and vertical offset estimation step, 12U CubeSat	125
4.25	Disturbance torque estimation step, 12U CubeSat	126
4.26	Attitude testing facility (including CubeDynA) residual torque level at varying CubeSat size. The bubble size represents the number of sensor stimulus, with 1 being only the air bearing.	129
4.27	Communication module test setup schematic	131
4.28	Synchronisation test, comparison of signals rising edges on oscil- loscope	132
4.29	Synchronisation test, delay with CAN interface	133
4.30	Synchronisation test, delay with I2C interface	134
4.31	Path delay test, delay with CAN interface	134
4.32	Path delay test, delay with I2C interface	135
4.33	Path delay test, delay with CAN interface, value at 1σ	135
4.34	Path delay test, delay with I2C interface, value at 1σ	136
7.1	Standard feedback configuration	149
7.2	Internal stability analysis diagram	150
7.3	Standard feedback configuration with weights	151
7.4	Diagram representation of lower LFT (left) and upper LFT (right)	152
7.5	LFT representation of the standard feedback configuration	153
7.6	Generalised interconnect plant configuration	154
7.7	General framework for the robust control synthesis problem	156
8.1	Simplified architectural diagram of the ESA high-performance bread- board	158
8.2	Plant with input multiplicative uncertainty	164
8.3	Standard feedback configuration with weights	166
8.4	Flowchart describing the process for obtaining a low order LTI controller which satisfies NP and RS. Y=Yes, N=No	168
8.5	Flowchart describing the process for obtaining a low order LTI controller which satisfies RP and RS. Y=Yes, N=No	168
8.6	Generalised interconnect plant configuration with scheduling pa- rameter p	170
8.7	Flowchart describing the process for obtaining a low order LPV controller which satisfies NP and RS. Y=Yes, N=No	171

9.1	Bode magnitude plot of frequency Response Function, inputs 1 to 4, breadboard accelerometer in position 1	176
9.2	Bode magnitude plot of frequency Response Function, inputs 5 to 8, breadboard accelerometer in position 1	176
9.3	Bode magnitude plot of frequency Response Function, inputs 1 to 4, zoom on frequency range, breadboard accelerometer in position 1177	
9.4	Bode magnitude plot of frequency Response Function, inputs 5 to 8, zoom on frequency range, breadboard accelerometer in position 1177	
9.5	Bode magnitude plot of frequency Response Function, inputs 1 to 4, breadboard accelerometer in position 2	178
9.6	Bode magnitude plot of frequency Response Function, inputs 5 to 8, breadboard accelerometer in position 2	178
9.7	Bode magnitude plot of frequency Response Function, inputs 1 to 4, zoom on frequency range, breadboard accelerometer in position 2179	
9.8	Bode magnitude plot of frequency Response Function, inputs 5 to 8, zoom on frequency range, breadboard accelerometer in position 2179	
9.9	Accelerometer signal, clipping is visible	180
9.10	Bode magnitude plot of FRF and of fitted state space models, inputs 1 to 3	182
9.11	Bode magnitude plot of FRF and of fitted state space models, inputs 4 to 6	183
9.12	Bode magnitude plot of the largest singular value of the difference between FRF and fitted state space model at varying frequencies and order. The maximum of the singular value is shown with filled circles	184
9.13	Bode magnitude plot of the maximum across frequencies of the largest singular value of the difference between FRF and fitted state space model at varying frequencies and order n.	184
9.14	Bode magnitude plot of FRE, fitted state space model and reduced models, inputs 1 to 3	185
9.15	Bode magnitude plot of FRE, fitted state space model and reduced models, inputs 4 to 6	185

9.16	Bode magnitude plot of the largest singular value of the difference between FRF and reduced state space model at varying frequencies and order. The maximum in the singular value is shown with filled circles	186
9.17	Bode magnitude plot of the maximum across frequencies of the largest singular value of the difference between FRF and the fitted state space model and of the difference between FRF and the reduced model at varying frequencies and order n.	186
9.18	Bode magnitude plot of the largest singular value of the difference between FRF and the fitted state space model and of the difference between FRF and the reduced model at varying frequencies and equal order	187
9.19	Bode magnitude plot of the optimal weight for uncertainty modelling and of weights of different order	188
9.20	Bode magnitude plot of the nominal model, model to cover and samples of uncertain model, inputs 1 to 3	189
9.21	Bode magnitude plot of the nominal model, model to cover and samples of uncertain model, inputs 4 to 6	189
9.22	Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for NP and RS at 0.30 frequency	193
9.23	Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for NP and RS at 0.30 frequency	194
9.24	Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for RP and RS at 0.30 frequency	194
9.25	Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for RP and RS at 0.30 frequency	195
9.26	Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for NP and RS at 0.42 frequency	195

9.27	Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for NP and RS at 0.42 frequency	196
9.28	Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for RP and RS at 0.42 frequency	196
9.29	Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for RP and RS at 0.42 frequency	197
9.30	Result of test of controller SISO LTI for NP and RS on the bread-board, disturbance frequency equal to 0.30	198
9.31	Result of test of controller SISO LTI for RP and RS on the bread-board, disturbance frequency equal to 0.30	198
9.32	Result of test of controller SISO LTI for NP and RS on the bread-board, disturbance frequency equal to 0.42	199
9.33	Result of test of controller SISO LTI for RP and RS on the bread-board, disturbance frequency equal to 0.42	199
9.34	Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for NP and RS at 0.30 frequency	202
9.35	Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for NP and RS at 0.30 frequency . . .	202
9.36	Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for RP and RS at 0.30 frequency	203
9.37	Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for RP and RS at 0.30 frequency . . .	203
9.38	Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for NP and RS at 0.35 frequency	204
9.39	Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for NP and RS at 0.35 frequency . . .	204

9.40	Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for RP and RS at 0.35 frequency	205
9.41	Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for RP and RS at 0.35 frequency . . .	205
9.42	Result of test of controller MIMO LTI for NP and RS on the breadboard, disturbance frequency equal to 0.30	208
9.43	Result of test of controller MIMO LTI for RP and RS on the breadboard, disturbance frequency equal to 0.30	208
9.44	Result of test of controller MIMO LTI for NP and RS on the breadboard, disturbance frequency equal to 0.35	209
9.45	Result of test of controller MIMO LTI for RP and RS on the breadboard, disturbance frequency equal to 0.35	209
9.46	Bode magnitude plot of the sensitivity function for the SISO LPV controller	211
9.47	Bode magnitude plot of the control sensitivity function for the SISO LPV controller	212
9.48	Result of test of controller SISO LPV controller in simulation, disturbance frequency varying linearly between 0.27 and 0.31	212
9.49	Result of test of controller SISO LPV controller in simulation, disturbance frequency varying in steps between 0.27 and 0.31	213
9.50	Result of test of controller SISO LPV controller on the breadboard, disturbance frequency varying linearly between 0.27 and 0.31 . . .	213
9.51	Bode magnitude plot of the sensitivity function for the MIMO LPV controller	215
9.52	Bode magnitude plot of the control sensitivity function for the MIMO LPV controller	216
9.53	Result of test of controller MIMO LPV controller in simulation, disturbance frequency varying linearly between 0.27 and 0.31 . . .	216
9.54	Result of test of controller MIMO LPV controller in simulation, disturbance frequency varying in steps between 0.27 and 0.31 . . .	217
9.55	Screenshot of the system identification GUI, multisine design initial page	220

9.56 Screenshot of the system identification GUI, model order reduction results	221
9.57 Screenshot of the controller synthesis GUI	222
9.58 Screenshot of the controller synthesis GUI, SISO LTI controller synthesis results	223
A.1 Coarse mass balancing result, case 1	233
A.2 Coarse mass balancing result, case 2	234
A.3 Coarse mass balancing result, case 3	235
A.4 Coarse mass balancing result, case 4	236
A.5 Coarse mass balancing result, case 5	236
A.6 Coarse mass balancing result, case 6	237
A.7 Coarse mass balancing result, case 7	238
A.8 Coarse mass balancing result, case 8	239
A.9 Coarse mass balancing result, case 9	239
A.10 Coarse mass balancing result, case 10	240
A.11 Coarse mass balancing result, case 11	241
A.12 Coarse mass balancing result, case 12	242
A.13 Coarse mass balancing result, case 13	242
A.14 Coarse mass balancing result, case 14	243
A.15 Coarse mass balancing result, case 15	244
A.16 Coarse mass balancing result, case 16	245
A.17 Coarse mass balancing result, case 17	245

List of Acronyms

ABT Air Bearing Table.

ACU Air Conditioning Unit.

ADCS Attitude Determination and Control System.

AFIT Air Force Institute of Technology.

AOCS Attitude and Orbit Control System.

APID Application Process IDentifier.

Astrofein Astro-und Feinwerktechnik Adlershof GmbH.

BST Berlin Space Technologies.

CAN Controller Area Network.

CCSDS Consultative Committee for Space Data Systems.

CM Centre of Mass.

CMG Control Momentum Gyroscope.

COTS Commercial-Off-The-Shelf.

CR Centre of Rotation.

CRC Cyclic Redundancy Check.

CSP Cubesat Space Protocol.

CubeDynA CubeSat Dynamics Attitude simulator testbed.

DOF Degrees Of Freedom.

DUT Device Under Test.

ECEF Earth-Centred-Earth-Fixed.

ECI Earth-Centred Inertial.

EM Engineering Model.

ESA European Space Agency.

ESTEC European Space Research and Technology Centre.

FIFO First-In First-Out.

FM Flight Model.

FRF Frequency Response Function.

FSM Fast Steering Mirror.

GC Geometrical Centre.

GNC Guidance Navigation and Control.

GNSS Global Navigation Satellite System.

GS Ground Station.

GSE Ground Station Equipment.

GUI Graphical User Interface.

HIL Hardware-In-the-Loop.

I2C Inter-Integrated Circuit.

IEC International Electrotechnical Commission.

IMU Inertial Measurement Unit.

IOD In-Orbit Demonstration.

ISO International Organization for Standardization.

JWST James Webb Space Telescope.

LAN Local Area Network.

LEO Low Earth Orbit.

LFT Linear Fractional Transformations.

LOS Line Of Sight.

LPM Local Polynomial Method.

LPV Linear Parameter-Varying.

LSQ Least-Squares.

LTl Linear Time-Invariant.

MGSE Mechanical Ground Station Equipment.

MIMO Multiple-Input Multiple-Output.

MIT Massachusetts Institute of Technology.

MTQ Magnetorquer.

NP Nominal Performance.

NPS Naval Postgraduate School.

NS Nominal Stability.

NTNU Norwegian University of Science and Technology.

NTP Network Time Protocol.

OBC On-Board Control Unit.

OBdH On-Board Data Handling.

OS Operating System.

OSI Open System Interconnection.

OSIE Open System Interconnection Environment.

PDU Protocol Data Unit.

PFM Proto-Flight Model.

PS Physical Stimulus.

PSU Power Supply Unit.

RMS Root Mean Square.

ROS Robot Operating System.

RP Robust Performance.

RQ Research Question.

RS Robust Stability.

RTN Radial-Transversal-Normal.

RW Reaction Wheel.

SDU Service Data Unit.

SGF Savitzky–Golay Filter.

SISO Single-Input Single-Output.

SNR Signal-to-Noise Ratio.

SPP Space Packet Protocol.

TC Telecommand.

TM Telemetry.

TRL Technology Readiness Level.

UART Universal Asynchronous Receiver-Transmitter.

UDP User Datagram Protocol.

UNSA Universidad Nacional de San Agustín de Arequipa.

USB Universal Serial Bus.

ZfT Space Systems Department Zentrum für Telematik.

Introduction

Background and motivation

The access to space benefits mankind in many ways, from weather forecast, disaster monitoring, to communication and global positioning. Except for a few examples of commercial use of space already in the 60's (Blair, 2012), (Puetite and Werth, 1971), the exploitation of space at its dawn was mostly limited to government agencies of a few countries (Sweeting, 2018). Starting from the 80's legislation such as the US Commercial Space Launch Act (US Congress, 1984) facilitated the commercialisation of space and also private companies started building and launching satellites. Launch cost, which is a significant part of a mission cost, did not change effectively between 1970 and 2000, while it started to significantly decrease from 2010 with the advent of SpaceX Falcon 9 launcher (Jones, 2018). Private investors started to play an increasing role in space enterprise funding in recent years (BryceTech, 2023), (OECD, 2023). Parallel to this trend, the size of satellites being launched has been decreasing. While the size of satellites peaked in the late 90's (Poghosyan and Golkar, 2017a), (De Sousa, 2023), the use of small satellites has become increasingly common thanks to electronic miniaturisation and their reduced cost (Sweeting, 2018). All these factors contributed to making space accessible to new players, such as Universities, small companies and to an increasing number of countries (OECD, 2023) and characterise the so-called “*newspace*” (Sweeting, 2018), (OECD, 2024)¹.

The CubeSat program, started from a collaboration between Stanford University and California Polytechnic State University in 1999 (Puig-suari et al., 2001), can be considered part of the newspace. CubeSats are satellites with standard size, based on 1 unit (or U), defined as a cube of 10 cm side, which can be released into orbit with a dedicated deployer. Born to be an educational tool for Univer-

¹Although newspace cannot be defined unambiguously (Golkar and Salado, 2021), (Denis et al., 2020), (Paikowsky, 2017), it can be characterised by unconventional system development approach and having private customers as primary market, as opposed to government customers (Golkar and Salado, 2021).

sity students to get hands-on experience on a real space mission, the CubeSats revealed to be much more than an academic exercise, with missions dedicated to Earth observation, communication, technological demonstration, exoplanet detection, and deep space (Polat et al., 2016), (Selva and Krejci, 2012), (Smith et al., 2018), (Saeed et al., 2020), (Villela et al., 2019a), (Schoolcraft et al., 2017), (Dotto et al., 2021). A CubeSat satellite's design is characterised by the use of Commercial-Off-The-Shelf (COTS) components (Woellert et al., 2011) and by a fast development (NASA, 2017).

The reliability of mission operated with such platforms can, however, be low (Villela et al., 2019a), (Swartwout, 2019). System functional testing is often overseen, especially by inexperienced teams (Swartwout, 2013), (CASGC et al., 2016). One of the systems critical for mission success, but also difficult to test at system level, is the Attitude Determination and Control System (ADCS). ADCS testing at system level requires reproducing the attitude dynamics of a satellite in space, characterised by very low torques. ADCS sensor and actuators rely on the external environment for their functioning. Simulation of solar radiation, Earth's magnetic field, and of starry sky is required to test respectively sun sensors, magnetometers, magnetorquers and star trackers. Additionally, other environmental factors influence the in-flight performance of the ADCS, such as Earth's albedo (Cantiello et al., 2022). The European Space Agency (ESA) supports CubeSat programs (ESA-TECSAA, 2022), (ESA-TEC, 2025), some of these missions suffer failures and have prolonged commissioning campaigns to resolve issues with the ADCS in-flight. A potential mitigation is to provide a verification and validation facility which can support a "test-as-you-fly" philosophy (ESA-TECSAA, 2022). Air bearing facilities for ADCS testing have been used since the beginning of the space age (Schwartz et al., 2003). Classical facilities, however, present levels of disturbance torque which are too high for smaller satellites (Nicolai et al., 2025), (Bahu, 2021). For an air bearing facility, it is critical to reduce the level of the gravity torque caused by the misalignment between the Centre of Mass (CM) of the rotating part and the Centre of Rotation (CR). There are examples of facilities being developed for CubeSat ADCS testing (Nicolai et al., 2025), (Tibbs, 2015), (Gavrilovich et al., 2015), (Chesi et al., 2015), but these facilities are often tailored to host a narrow range of CubeSats sizes and offer limited sensor stimulus capabilities.

The need for a new test facility which can host a wide range of CubeSats and

provide sensor stimulus and Hardware-In-the-Loop (HIL) control for the most commonly used CubeSat ADCS actuator and sensors arises and it is at the basis of the first Research Question (RQ) 1 of the doctorate work:

Research Question 1. *“Is it possible to design and realise a CubeSat ADCS test facility capable of hosting CubeSats of different sizes and enabling HIL testing under realistic environmental stimuli?”*

The RQ1 is linked to a request for proposal of the ESA, which confirms the need of such a facility (ESA-TECSAA, 2022). Strictly linked to RQ1, another research question can be formulated, which addresses the performance of such a facility:

Research Question 2. *“What level of disturbance torque can be obtained by a facility designed to test the ADCS of CubeSats of different sizes?”*

To realistically emulate the attitude dynamics of a satellite in space, the disturbance torque should be maintained to a low level. For CubeSat, the level of disturbance torque can be as low as $10^{-6} Nm$ (Gavrilovich et al., 2015). Obtaining a low torque level in a laboratory environment is a challenging objective, and it has been tackled as part of the doctorate work.

Beyond reliability, performance limitations also affect small satellites due to their reduced dimensions. The performance of an optical payload, for instance, is physically limited by the dimensions of its components (limit on focal length and lens diameter) (Janschek et al., 2007), actuator size is limited as well. Nevertheless, the performance requirements for satellites with optical payloads have been increasing over the last years (Bhatia, 2021). To guarantee such levels of performance, high pointing accuracy and an ultra-quiet environment are needed. Onboard satellites, active components such as reaction wheels and cryocoolers generate mechanical vibrations which can hinder the achievement of such an environment (Le, 2017). At the same time, the satellite’s structure is becoming lighter and more flexible, and microvibrations can be amplified if they interact with the spacecraft’s structural model (Le, 2017). Passive isolators, such as elastomers, are used for vibration isolation. They suppress microvibration at high frequency, but low frequency disturbances can easily propagate through (De-

merville and Bastia, 2011) and are even amplified around the flexible modes introduced by these isolators (Preda, 2017). A promising alternative is a hybrid isolation strategy that combines passive elements with active control, extending vibration suppression across a broader frequency band. The ESA GNC, AOCS & Pointing Laboratory has developed a high-performance stabilisation breadboard used to validate and assess the pointing stability performances of similar future systems in the presence of microvibrations. The system mitigates wheels and cryocoolers micro-vibrations using shakers and accelerometers and/or gyroscopes, placed either below the payload or the disturbance source.

In the context of hybrid isolation, different types of controller algorithms have been designed, tuned, and tested in simulation. Some have been validated on a given breadboard configuration (Bloemers et al., 2024), (TU/e, 2024). However, each new breadboard configuration still requires system identification and controller re-tuning, a process that remains largely manual and time-consuming. This fact leads to the final research question of this doctoral work:

Research Question 3. *“What vibration reduction performance can be achieved through an automated control-tuning process on a hybrid passive-active pointing stability testbed with an unknown mechanical configuration?”*

Structure of thesis

The thesis is divided into two main parts: research conducted on the CubeSat testing facility (Part I) and on the high-performance stabilisation breadboard of ESA/ESTEC (Part II).

In **Part I**, the development, realisation and testing of the CubeSat testing facility mass balancing system is described. The mass balancing system is required to reduce the level of disturbance torque during testing of the satellite attitude determination and control system. It acts by shifting the position of the CM to make it coincide with the CR. It is composed of a manual coarse balancing part and a fine automatic balancing part. The fine balancing is achieved by dynamically commanding sliding masses' position in closed loop and by estimating the platform inertia. The mass balancing system has been designed and tested against the requirement specifications. It will be shown that, by using semi-

automatic procedures for mass balancing, the level of disturbance torque acting on the facility can be brought down to a magnitude comparable with the state of the art. Additionally, the description and testing results of the facility communication module, used to enable wireless communication between the satellite and ground station, are reported.

In **Part II**, the validation of the ESA high-performance stabilisation breadboard is described. The procedure consists in characterising the system dynamics through a system identification procedure and in testing different types of controllers on the breadboard. The system identification is realised by a data-driven approach which utilises experimental data to obtain a non-parametric and a parametric linear model of the system. Based on this model Linear Time-Invariant (LTI), Linear Parameter-Varying (LPV), Single-Input Single-Output (SISO) and Multiple-Input Multiple-Output (MIMO) controllers are designed using closed loop shaping techniques. These controllers are then deployed and tested on the breadboard. The whole procedure is automatised and made user-friendly with a Graphical User Interface (GUI).

Each part of the thesis reports the background and motivation, the methodology adopted, the results obtained and the conclusions.

Notation used

Throughout the thesis, the following notation and symbols are used:

- Vectors are indicated with bold characters, $\mathbf{v} \in \mathbb{R}^n$; their corresponding vector norm are indicated with plain characters $v = \|\mathbf{v}\|$.
- A vector with unit norm is indicated as $\hat{\mathbf{v}}$ and also referred to as unit vector.
- Matrices are indicated with plain characters and capitalised M .
- The symbol $\mathbf{v}^T$ (M^T) denotes the transpose of the vector $\mathbf{v}$ (matrix M).
- The symbol $\mathbf{v}^*$ (M^*) denotes the complex conjugate transpose of the vector $\mathbf{v}$ (matrix M).
- If not otherwise specified, it is assumed that $\mathbf{v}$ is a column vector.

- When a vector is expressed in a reference frame, a superscript is added to the notation. The vector $\mathbf{v}$ expressed in an inertial and in a body reference frame are denoted respectively as $\mathbf{v}^i$ and $\mathbf{v}^b$. When no superscript is specified, the vector is intended to be a physical vector, unless specified otherwise in the text.
- A reference frame is indicated with a capital letter and calligraphic font, $\mathcal{F}_i$ indicates an inertial frame of reference, $\mathcal{F}_b$ indicates a body-fixed frame of reference. The frame of reference can be specified by listing the elements of its basis, intended as a basis of a vector field.
- Transformation between frames of reference and in particular rotations are indicated with the symbol R_i^b where the subscript indicates the original frame of reference and the superscript indicates the final frame of reference. Rotations are to be intended as intrinsic passive rotations.
- The $r \times r$ identity matrix is denoted by I_r , the I_3 identity matrix is denoted simply by I :
- The inertia matrix is denoted by J :

$$J = \begin{bmatrix} J_{xx} & J_{xy} & J_{xz} \\ J_{yx} & J_{yy} & J_{yz} \\ J_{zx} & J_{zy} & J_{zz} \end{bmatrix} \quad (0.1)$$

- The scalar product between the vector $\mathbf{v} = [v_x, v_y, v_z]^T$ and the vector $\mathbf{w} = [w_x, w_y, w_z]^T$ is denoted by:

$$\mathbf{v} \cdot \mathbf{w} = \mathbf{v}^T \mathbf{w} = v_x w_x + v_y w_y + v_z w_z \quad (0.2)$$

- The symbol $[\mathbf{v}^\times]$ indicates the skew symmetric operator which acts on the vector $\mathbf{v} \in \mathbb{R}^3$, and results in a 3×3 matrix:

$$[\mathbf{v}^\times] = \begin{bmatrix} 0 & -v_z & v_y \\ v_z & 0 & -v_x \\ -v_y & v_x & 0 \end{bmatrix} \quad (0.3)$$

with v_x , v_y , and v_z vector coordinates of $\mathbf{v}$ in a given basis.

The skew-symmetric operator can be used to represent the cross product between a vector $\mathbf{v}$ and a vector $\mathbf{w}$:

$$\mathbf{v} \times \mathbf{w} = [\mathbf{v}^\times] \mathbf{w} = \begin{bmatrix} v_y w_z - v_z w_y \\ v_z w_x - v_x w_z \\ v_x w_y - v_y w_x \end{bmatrix} \quad (0.4)$$

- The symbols $\bar{\sigma}(M)$ and $\underline{\sigma}(M)$ denote respectively the largest and the smallest singular value of M .
- j denotes the imaginary unit.
- s denotes the Laplace variable
- $Re(s)$ denotes the real part of s , $Im(s)$ denotes the imaginary part of s .

Part I:
The CubeSat Attitude Testing
Facility at ESA/ESTEC

1

Part I, Introduction

The definition of small satellite or smallsat is not standardised and has changed over the years ([NASA, 2025](#)). NASA defines a smallsat as a satellite with mass up to 180 kg ([NASA, 2024](#)). Within the smallsat definition, different subcategories are defined based on the satellite mass:

- Minisatellite, 100 kg to 180 kg
- Microsatellite, 10 kg to 100 kg
- Nanosatellite, 1 kg to 10 kg
- Picosatellite, 0.01 kg to 1 kg
- Femtosatellite, 0.001 kg to 0.01 kg

The CubeSats are a particular class of nanosatellites and microsatellites, with standardised size and mass. Among small satellites, CubeSats have gained popularity in the last 20 years, with an increasing number of satellites deployed into orbit and upcoming launches (see Figure 1.1). The increase in adoption rate of this standard is remarkable, it took 15 years to reach 1000 CubeSats launched (mid-2003 to mid-2018) and just 4 years to reach 2000 units (mid-2022) ([Kulu, 2024](#)). One of the key factors behind this success is the creation of a standard

itself. A small and standardised platform indeed helps reducing the development cost and lowering the entry barrier to the market (NASA, 2017), allows adopting a common deployment system (Poghosyan and Golkar, 2017b), (Villela et al., 2019b) and incentives the commercialisation of (COTS) components (CASGC et al., 2016). The CubeSat standard was created and is updated by the California Polytechnic State University (SLO, 2025) and has recently become an ISO standard (ISO, 2017). The guidelines specify different form factor multiples of a single unit (e.g. 1U, 3U, 12U), but it is common to combine different units to develop satellites which go beyond the guidelines (e.g. 16U, 24U) (Burkhard, 2021), (EnduroSat, 2025). The low cost of CubeSats makes their use attractive for constellations, with Planet, Spire and Swarm constituting the largest constellations (Kulu, 2024). While the first CubeSats were developed for the largest part by University teams, over the years, the number of CubeSats developed by private companies increased (Kulu, 2024). The main drawback of CubeSats is probably the fact that mission success for these types of missions is limited. A few extensive studies indeed report on the limited reliability of CubeSats missions (Swartwout, 2019), (CASGC et al., 2016), (Kulu, 2024), (Villela et al., 2019b), (Polat et al., 2016). The experience of the development teams is often cited as one of the driving factors behind this low success rate, with it improving significantly with the second satellite developed (CASGC et al., 2016), (O'Donnell and Richardson, 2020), (Bouwmeester et al., 2022). The lack of functional system level testing is another factor that limits the reliability of these missions (Swartwout, 2019), (Swartwout, 2013), (Bouwmeester et al., 2022), (Venturini et al., 2018).

The ADCS is a critical subsystem on board of satellites and one of the most complex to be tested at system level. Its testing indeed requires the 3-Degrees Of Freedom (DOF) rotational motion with environmental conditions close to space (Chesi et al., 2015), (Gavrilovich et al., 2016). Air bearing facilities for HIL testing have been used since the dawn of space age (Schwartz et al., 2003). Test facilities developed to test traditional, bigger satellites are not adapted to test nanosatellites because their level of disturbance torques (Nicolai et al., 2025), (Bahu, 2021). In recent years different facilities for nanosatellites ADCS testing have been developed (Tibbs, 2015), (Gavrilovich et al., 2015), (Chesi et al., 2015), (Prinkey, 2013), (Ustrzycki, 2011), (Li et al., 2013), (Silva et al., 2019), (Modenini et al., 2020), (Wolosik, 2018), (Lavezzi et al., 2022), (BST, 2024), (Astrofein,

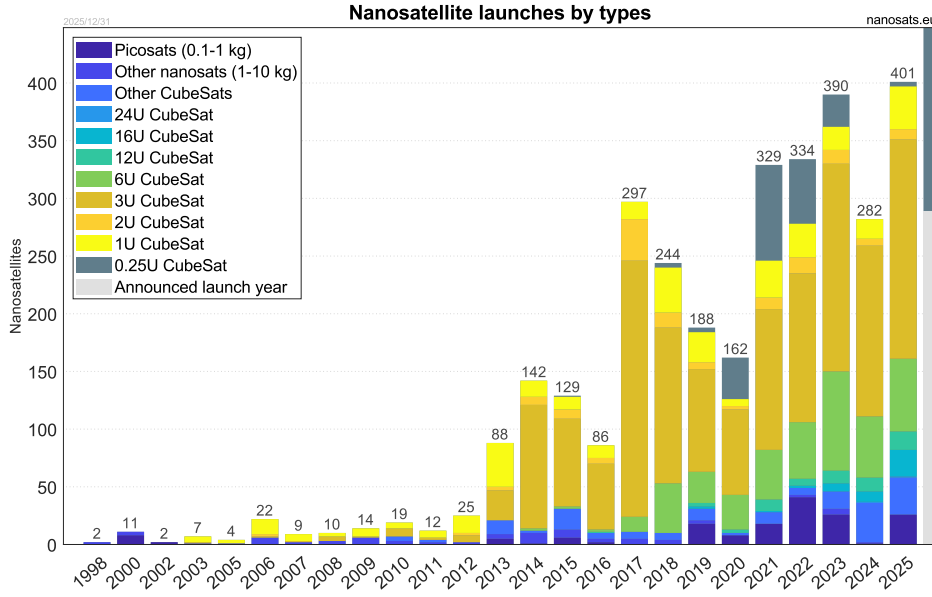


Figure 1.1: Nanosatellites launched by type, 1998 – 2025 (Kulu, 2025)

2025), (Mittelsteadt and Mehiel, 2007), (Contreras et al., 2025), (Wallshauser et al., 2025). Apart from a few examples (Modenini et al., 2020), (Astrofein, 2025), (Sputnix, 2020), no facility is designed to test flight models of CubeSats of various sizes (1U to 6U or 12U) with specified levels of disturbance torque and various sensor stimuli (e.g. magnetic, Sun, albedo simulation). The realisation of such a facility can significantly contribute to improving the success rate of mission operated with CubeSat through ADCS HIL functional testing. In this context, ESA supported the development of a 3-DOF attitude control air bearing facility to support a “test-as-you-fly” approach (ESA-TECSAA, 2022). The development of the **CubeSat Dynamics Attitude simulator testbed (CubeDynA)** is an answer to this need.

The CubeDynA facility has been developed by the **Microsatellites and Space Microsystem Laboratory (u3S)** of the **Alma Mater Studiorum Università di Bologna** and its spin-off **Nautilus Navigation in Space**. The novelty of the CubeDynA facility allows testing CubeSats of sizes of 1U, 3U, 6U and 12U with a low level of disturbance torque and with simulation of Earth’s magnetic field, Sun simulation, albedo simulation, a ground truth system and possibly start tracker surrogate measurements. The development spanned about 2 years and included the contribution of the following people: Alfredo Locarini as project manager,

1.1. CubeSat characteristics and technology

Dario Modenini as technical manager, Giacomo Curzi, Alessandro Lotti, Daniele Pecorella and the PhD candidate Andrea Curatolo as project engineers. In particular, the PhD candidate has contributed to the development, assembly and testing of the mass balancing system and of the communication interface between ground station and Device Under Test (DUT), to the commissioning of the facilities at European Space Research and Technology Centre (ESTEC) premises and to training of ESA personnel.

Part I of the thesis is organised as follows: the remainder of Chapter 1 is dedicated to a description of CubeSats' architecture, ADCS and testing methodologies. A review of the existing CubeSat testing facilities is presented and the existing gap is highlighted. Chapter 2 describes the theoretical preliminaries used in this part of the thesis. Chapter 3 presents the facility design starting from the requirements, with focus on the balancing system and communication module. Chapter 4 shows the result of the facility performance evaluation. Chapter 5 draws the conclusion for the first part of the thesis.

1.1 CubeSat characteristics and technology

The CubeSat standard is based on 1 unit (1U): a 10 cm cube with mass up to 2 kg (SLO, 2025). The current standard specifies CubeSats of size 1U, 1.5U, 2U, 3U, 6U and 12U (see Figure 1.2) and possibly extra volume for the 3U, 6U and 12U CubeSats. The design specification document provides mechanical, electrical and operational specifications as well as testing requirements. Of particular interest for the facility development are the specifications in terms of mass and CM position relative to their Geometrical Centre (GC). The control of the position of the CM of the satellite and testing platform is indeed important to reduce the disturbance gravity torque.

The CubeDynA facility is designed to host CubeSat of 1U, 3U, 6U and 12U sizes, their mass, size and CM allowed location is reported in Table 1.1. The coordinate axes definition for the CubeSats is shown in Figure 1.3. Given their mass, the CubeSats belong to the class of nanosatellite and microsatellite but often the terms CubeSats and nanosatellite are used interchangeably.

When it comes to the ADCS of CubeSats, many of the sensors and actuator types used on traditional satellites are also adopted. These include direction sensors

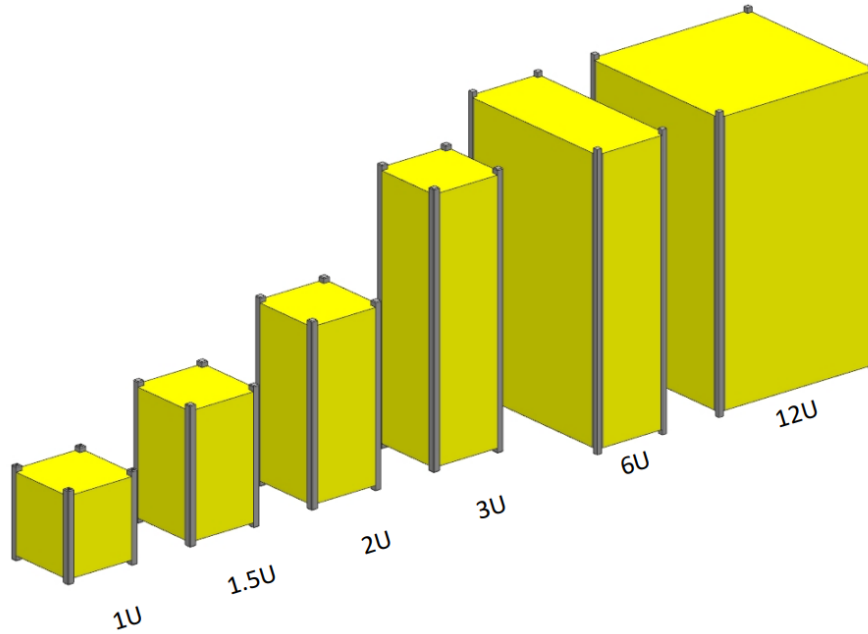


Figure 1.2: CubeSat sizes listed in the CubeSat design specification (SLO, 2025)

like Sun sensors, magnetometers, Earth horizon sensors, star trackers and inertial sensors such as gyroscopes; while reaction wheels, magnetic torques and, less often, thrusters and Control Momentum Gyroscope (CMG) are used as actuators (NASA, 2025), (Polat et al., 2016), (Xia et al., 2017). Some of the most commonly used sensors and actuators for CubeSats are presented here.

Sun sensors are used to detect the direction of the Sun in the spacecraft body frame. Different technologies are used for CubeSats' Sun sensor, these include cosine director photocells, quadrant detectors, digital Sun sensors and Sun cameras (NASA, 2025). Care must be taken to ensure that the Moon and Earth's albedo do not influence the Sun direction estimation; this can be done with a threshold on the brightness level, with baffles for instance. Sun sensors provide just one direction, so they cannot be used alone for three-axis attitude determination, but they must be coupled with at least another type of sensor.

Magnetometers provide a measure of the local magnetic field, which is compared with an on-board model of the external magnetic field. For this reason, they are especially suited for Low Earth Orbit (LEO) for which the Earth's magnetic field is still strong and the magnetic field has been characterised. They should be mounted far from sources of magnetic fields, which can be challenging given the limited size of CubeSats. Sometimes they are mounted on a boom

1.1. CubeSat characteristics and technology

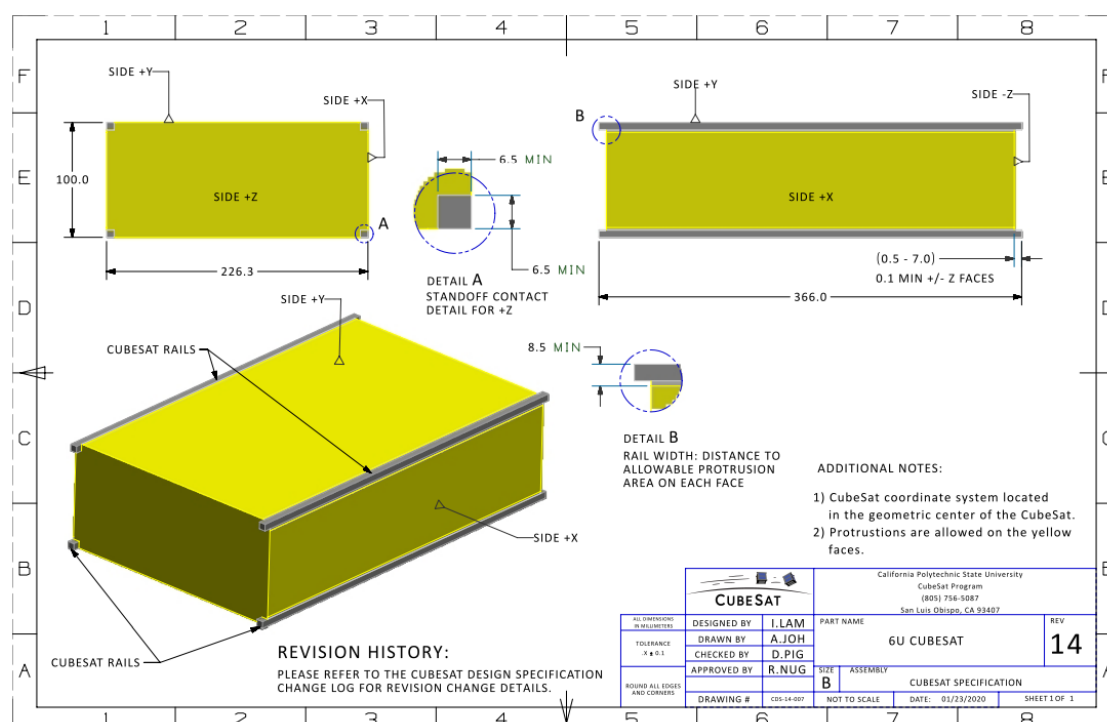


Figure 1.3: CubeSat coordinate axes definition in the CubeSat design specification (SLO, 2025)

outside the spacecraft. As for the Sun sensors, magnetometers cannot provide alone three-axis attitude estimate but need to be coupled with another type of sensor. They can be used to provide angular rate information if no other direct measurement is present during detumbling (b-dot law, [Stickler and Alfriend \(1976\)](#)).

Earth's horizon sensors provide the nadir direction by detecting the Earth's limb in infrared or by measuring the temperature differences between the poles and the equator. They can be static or they can scan the sky. As for the Sun sensors and magnetometers, they provide just one direction measurement.

Star trackers provide an accurate three-axis attitude estimate by looking at the starry sky and comparing the image with a star catalogue. They are the most accurate and expensive sensors and are employed on missions that require great accuracy. Their measurements can be corrupted by other sources of light in the field of view (straylight), so baffles are often used, and their placement is carefully selected at a mission design level. In recent years, the number of miniaturised star trackers especially developed for CubeSats has increased.

Table 1.1: CubeSat mass, size and allowed CM to GC distance

CubeSat	Mass [kg]	Size [mm] $x \times y \times z$	Allowed distance CM to GC [mm]
1U	2	$100.0 \times 100.0 \times 113.5$	x axis: +20 / -20 y axis: +20 / -20 z axis: +20 / -20
3U	6	$100.0 \times 100.0 \times 340.5$	x axis: +20 / -20 y axis: +20 / -20 z axis: +70 / -70
6U	12	$226.0 \times 100.0 \times 366$	x axis: +45 / -45 y axis: +20 / -20 z axis: +70 / -70
12U	24	$226.3 \times 226.3 \times 366$	x axis: +45 / -45 y axis: +45 / -45 z axis: +70 / -70

Inertial sensors include **gyroscopes** for angular velocity measurement and **accelerometers** for rate of velocity change measurements. They can be used during detumbling of the spacecraft, to propagate the attitude in-between direction sensors measurements and more in general for attitude control. Gyroscopes used on CubeSats are often of the MEMS type rather than the fibre optic gyro type because of their compact size, at the cost of reduced accuracy. Different types of errors affect gyro measurements, these include, in addition to random noise and bias, bias instability and angular random walk.

Magnetic torquers or **Magnetorquer (MTQ)** are actuators which produce a dipole which interacts with the external magnetic field to change the spacecraft's attitude. They can be air coils or torquer bars/rods; in this last case, a ferrous core is added to amplify the strength of the generated dipole. Each type offers different advantages and disadvantages in terms of power consumption, residual magnetisation and size/mass. One main disadvantage of magnetorquers is that they cannot provide a torque in the direction of the external magnetic field. So in principle, three-axis control cannot be achieved, if not coarsely, when the external magnetic field direction changes over time as the spacecraft travels along the orbit. One advantage is the absence of moving parts and so a reduced risk of failure. They are often used for detumbling or for reaction wheels desaturation. **Reaction** and **momentum wheels** use the principle of conservation of angular

1.1. CubeSat characteristics and technology

momentum to change the attitude of the spacecraft. They consist of a flywheel spun by an electric motor, which can be used to exert a torque on the spacecraft and to store angular momentum. They can provide three-axis attitude control and together with torquer coils, are the most used form of actuation on board of CubeSats (Polat et al., 2016). A few disadvantages of this form of actuation consist in the need to perform periodic desaturation manoeuvres to reduce the wheel speed by using another form of actuation (e.g. magnetorquers) and the possible disturbance that they can induce because of imperfect rotor balancing (an aspect addressed in Part II of the thesis).

These sensors and actuators are sometimes combined in an integrated ADCS unit, which also includes processors and software with attitude determination and control capabilities. It is not rare, however, to integrate different COTS components in-house.

1.2 CubeSat development, reliability and testing

The initial motivation for the development of CubeSat was educational (Puigsuari et al., 2001), so the development, launch and operational cost was kept low (Villela et al., 2019b). Even if today CubeSats are far more than an educational tool (CASGC et al., 2016), (Poghosyan and Golkar, 2017b), the minimisation of the cost is still pursued. This is done in part by using COTS components, which also helps in keeping the team small and in part by shrinking the test campaigns (Villela et al., 2019b). The compressed development schedule can lead, however, to uncaught mistakes (Swartwout, 2019). The possibility of launching CubeSats as secondary payloads is one factor that helps keep the cost down (CASGC et al., 2016).

The lower success of CubeSats mission compared to satellites which follow the traditional development process has been analysed in many studies (CASGC et al., 2016), (Kulu, 2024), (Villela et al., 2019b), (Polat et al., 2016), (Bouwmeester et al., 2022), (Venturini et al., 2018). A common difficulty in realising these studies is the absence of updated data on the mission status (Swartwout, 2019), (Kulu, 2024), so it is realistic to think that the success rate is even lower than what estimated, as often no news equals bad news. A study from 2019 shows that, as per 2018, 61% of CubeSat missions were successful, 21% experienced satellite's failure, 2 % were not ejected, 12 % experienced a launch failure and for the remaining 4% the mission status was unknown (Villela et al., 2019b). A more recent 2024 study indicated that out of 2714 launched CubeSats, 677 of them (25%) had at least one issue or failure and 200 of them (7%) have an unknown status, indicating a very likely failure (Kulu, 2024). A source of failure is the inexperience of the teams, O'Donnell and Richardson (2020) found that this is indeed the most important predictor for success. What also results from the studies is that because of the compressed time-to-launch, extensive testing is often overseen. Bouwmeester et al. (2022) compared the impact of subsystem redundancy and improved testing on CubeSats' reliability and found that the latter is superior to the former in reducing immature failures. Swartwout (2019) found that a development schedule which allows significant functional testing and margin is a common characteristic of the success of CubeSat missions. A "fly-learn-refly" approach, in which two flight models of the CubeSat are developed and the sec-

1.2. CubeSat development, reliability and testing

ond is modified based on the experience of the first flight, is sometimes used and helps improving reliability (CASGC et al., 2016). A trade-off of this approach with extensive testing should, however, be done.

When it comes to ADCS testing, the ESA Engineering guidelines for CubeSat IOD projects provide a list of tests which are needed/recommended (ESA-TEC, 2025). **Polarity test** or sign test are used to verify the correct polarity of the Attitude and Orbit Control System (AOCS) chain from sensor to actuators (European Cooperation for Space Standardization, 2022) and are highly recommended by ESA guidelines. This test should be conducted on the **Proto-Flight Model (PFM)** or on the **Flight Model (FM)** for each functional chain per AOCS mode (ESA-TEC, 2025). No end-to-end polarity check is one of the issues identified by ESA that lead to corrective hardware or software measures during assembly integration and testing or in-flight, if not the near loss of mission (Cantiello et al., 2022). To provide physical sensor stimulus ESA suggests using: a torch/lamp or Sun (outdoor) for Sun sensors, Helmholtz cage or Earth-field data to test magnetometers, rotation of the spacecraft for testing gyroscopes using e.g., an air bearing table, optical stimulus of star trackers. The use of an air bearing table is suggested (along with other solutions) for observing the physical actuator response (e.g. reaction wheels, magnetorquers). A **Integration/Functional test** for all AOCS units is also suggested. This includes the test of Sun sensors with a Sun emulator and of magnetometer calibration with a Helmholtz cage. **HIL** closed-loop testing is highly recommended if resources allow. This can consist of a test-as-you-fly test with the full AOCS or CubeSat on a 1-DOF or 3-DOF air bearing table.

A HIL ADCS testing facility addresses the functional and polarity testing problem and can be a useful tool to correct issues with the ADCS before they manifest in-flight.

1.3 Review of CubeSat attitude testing facilities

To test an ADCS HIL, it is necessary to recreate the free rotational motion of a satellite in space. This is characterised by very low torques, which on nanosatellites can be on the order of 1×10^{-6} N m. These torques can be external, caused by the Earth's gravitational and magnetic field, the Earth's atmosphere, the Sun radiation pressure or internal, caused by changes in spacecraft mass distribution. Recreating an environment which allows a satellite to rotate with such a low level of external torques is not an easy task. Historically, different methods have been employed to obtain representative attitude simulation. One of them consists of letting the satellite fall from a height and be caught by a soft net. Clearly, in this method the testing time is limited by the height and can be of a few seconds (Prado et al., 2005), (Boynton, 1996). Parabolic flights are another possibility, but the flight time is limited to 26 s (Prado et al., 2005) and the cost can be high. An alternative and low-cost approach consists of suspending the satellite with thin wires (Lavezzi et al., 2022), (Probe et al., 2016). This method is reported to allow a rotating range comparable to air bearing-based floating testbeds and a low torque level, even if its value is not specified. The most widely adopted approach consists, however, of the use of spherical air bearing-based facilities. Compared to free-fall tests and parabolic flights, the simulation time can be much longer, virtually unlimited. In reality, parasitic torques and rotation range limits reduce the total simulation time to the order of hours. Such a facility consists of a spherical joint in which pressurised air flows and of an Air Bearing Table (ABT), which hosts the payload to be tested. The payload can consist of an ADCS mock-up or a satellite flight model. The use of an air bearing drastically reduces the friction torque. A drawback of air bearing facilities consists in the fact that whenever the (CM) of the rotating part of the structure is not coincident with its CR, a gravity torque is present (Boynton, 1996). If not taken into account, this torque hinders the possibility of conducting representative tests. The torque is not present, however, when the CM and CR lie on the same vertical line, this condition is called **static balance**, meaning the torque is null for just the given attitude. If instead the CM and CR positions coincide, the condition of **dynamic balance** holds, which is independent of the attitude. Considering the static balance, two equilibrium positions exist: when the CM is

1.3. Review of CubeSat attitude testing facilities

below the CR and when it is instead above. In the first case, the system acts like a clock pendulum and the equilibrium is **stable**. When instead the CR is above the CR, the equilibrium is **unstable**, and the system will diverge from it, with the platform eventually reaching its angular range limit. The CR position is defined by the geometry of the air bearing. Air bearings of different radii and height can be manufactured to regulate the CR position (Boynton, 1996), but once the air bearing is manufactured, the CR position is fixed. In this case, the only possibility to reduce the gravity torque is to shift the CM position. This can be done by distributing the weights of the components in case of an ADCS mock-up for which there are fewer constraints on the components' position. For a FM, however, components' position is fixed and a mass balancing system is needed. The balancing system usually consists of counterweights distributed to bring the CM position close enough to the CR so that the gravity torque's level is reduced to acceptable levels. The compensation can be manual or automatic, with the latter usually providing finer CM position control and hence a lower torque level. A drawback of using additional mass is that the inertia of the rotating structure is no longer representative of that of the satellite.

Depending on the design of the ABT, a classification can be done (Schwartz et al., 2003), see Figure 1.4 for reference and for yaw, pitch and roll angles definition:

- **Tabletop**: the air bearing consists of a spherical cap and the ABT is mounted right above the air bearing. This type of ABT offers unlimited angular motion in the yaw axis but limited in pitch and roll;
- **Umbrella**: the air bearing is a full sphere and the ABT is interfaced with it through an extension rod. This configuration, similarly to the tabletop one, offers unlimited yaw axis motion and limited but greater range of motion in pitch and roll, at the cost of an increase in system complexity;
- **Dumbbell**: the air bearing is a full sphere, the ABT consists of two tables at two opposing side. This design allows unconstrained motion in yaw and roll axes, while the motion in pitch is limited. In this case, the components are split on the two tables, allowing the CM position to be placed closer to the CR. This configuration however does not allow to test flight models since these cannot be divided into two separate pieces.
- **Hollow**: the air bearing hemisphere is hollow and the components are

placed inside of it. It has the advantage of minimising the CM to CR distance but the available space is very limited

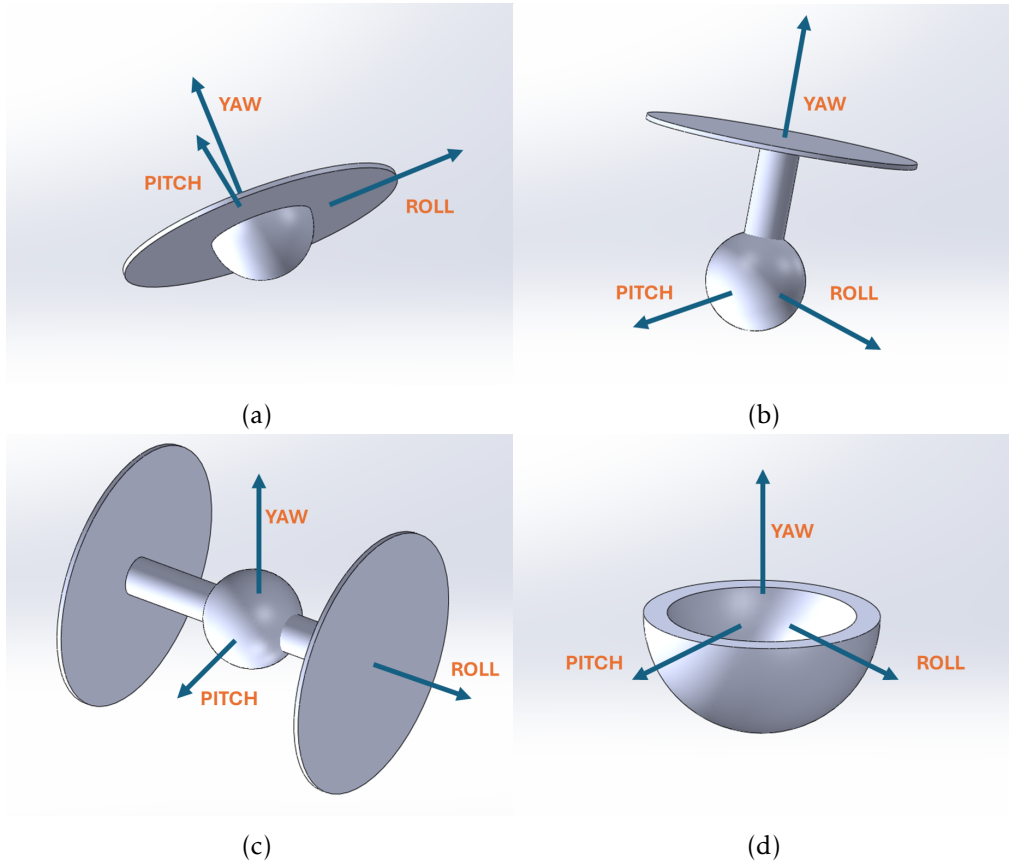


Figure 1.4: Different types of ABT: Tabletop (a), Umbrella (b), Dumbbell (c), Hollow (d)

There are a few examples of air bearing facilities being developed since the 60's, ([Schwartz et al., 2003](#)) offers a historical review of such facilities. Facilities from the 90s were intended to emulate large satellite ADCS, for instance, the air bearing of ASTREX of Edwards Air Force Base, California ([Das et al., 1990](#)) could support a load of 6600 kg. Nevertheless, some examples of small platforms could also be found, such as the Small Satellite Attitude Control Simulator, developed by the Utah State University, with a load of 80 kg, gas jet thrusters as actuator, gyro, Sun sensor and magnetometer as sensor ([Fullmer et al., 1992](#)). These testbed also included a mass balancing system, but the level of achieved gravity torque is not reported. The testbed developed in 2003 by the Georgia Institute of Technology instead reports an estimated gravity torque of $6.8 \times 10^{-2} \text{ N m}$. It

1.3. Review of CubeSat attitude testing facilities

had a total weight of 134 kg. It used CMG, thrusters and Reaction Wheel (RW) as actuators, accelerometers, Sun sensors, gyros and magnetometers as sensors (Jung and Tsiotras, 2003). The three-axis air-bearing-based platform of the Institute of Geography, UNAM, specifically addresses small satellites and reaches 2×10^{-4} N m residual torque level. It includes RWs and magnetic coils as actuators, Sun, Earth sensors, magnetometer and Inertial Measurement Unit (IMU) as sensors (Prado et al., 2005). Other examples of testbeds which do not specifically address small satellites can be found in Mittelsteadt and Mehiel (2007), Jung and Tsiotras (2003), Peck et al. (2003), Al-Majed and Alsuwaidan (2009), Gallardo et al. (2011), Kabganian et al. (2015), Agrawal and Rasmussen (2001), Xu et al. (2015), Tavakoli et al. (2017), Da Costa et al. (2019), Sharifi et al. (2019). The increase in the adoption of CubeSats for missions of growing complexity poses the problem of scaling these facilities down to the micro- and nanosatellite domain. The level of disturbance torque acting on facilities designed to test larger satellites is not adequate for CubeSats. Starting from 2013, facilities specifically designed for testing CubeSats or nanosatellites have been developed. As part of the present work, a review of these facilities is performed. The capabilities of attitude testing facilities are compared in terms of:

- payload that can be tested;
- which characteristics of the space environment are reproduced;
- the residual gravity torque.

This review highlights a gap in existing facilities in terms of hosting capability, environment simulation capability, and achievable disturbance torque. The research presented in this thesis describes the development of a facility which addresses this gap.

Based on what it is tested, the definitions of *emulation* testbed and *simulation* testbed are introduced:

- in an emulation testbed, the satellite ADCS is not directly tested, rather its behaviour is emulated using a set of sensors and actuators which are part of the testbed itself;
- in a simulation testbed instead, the whole satellite Engineering Model (EM), FM, PFM or its ADCS are tested in closed-loop.

While emulation testbeds tend to be more compact and allow for distributing the actuators and sensors evenly on the ABT, easing the balancing process, a simulation testbed better addresses the need of performing system-level testing on PFM or FM. Furthermore, a simulation testbed allows for hosting different sizes of CubeSats.

The existing CubeSat 3-DOF testbeds, to the best of the author's knowledge, are listed in Table 1.2. The table reports the developer of the facility together with the last publication year, the scope of the testbed, being it simulation or emulation, the testbed type, which actuators and sensors are present, the hosting and simulation capabilities, the type of mass balancing, being it manual or automatic, and the residual gravity torque level. The results of the mass balancing are reported only if this has been experimentally tested, as opposed to being tested solely in simulation.

The **Massachusetts Institute of Technology (MIT)** Space Systems Laboratory developed a testbed specifically intended to test the MicroMAS 3U CubeSat. It includes a 4-coil Merritt cage (a variation of a Helmholtz cage) and 3-DOF air bearing (Prinkey, 2013). The paper mainly focuses on the cage development, while no mention is made of the mass balancing method or residual gravity torque level. The spherical air bearing appears to be of the tabletop type. Nanosatellite Three Axis Attitude Control Simulator (NanoTAACS) of **York University** consists of a spherical air bearing and a tabletop ABT (Ustrzycki, 2011). It includes eight threaded rods for mass balancing. The performance reached by the balancing system is not reported, but it is mentioned that initial balancing is able to bring the CM at a 8.88×10^{-4} m distance from the CR. Considering the total mass of 3.8 kg, this corresponds to a gravity torque of 3.3×10^{-2} N m. The testbed is used to test a 1U CubeSat ADCS (Li et al., 2013). The CubeSat Three-Axis Simulator (CubeTAS) of the **Naval Postgraduate School (NPS)** is a testbed developed to emulate CubeSat ADCS (Chesi et al., 2015), and it is claimed to be the first CubeSat testbed ever built (Meissner, 2009). It consists of a hollow spherical air bearing containing three reaction wheels, a Sun sensor, an IMU, three magnetorquers an On-Board Control Unit (OBC) and a battery. A Helmholtz cage and a Sun simulator are used for sensor stimulus. A metrology system is also present for attitude determination. The mass balancing is automatic and it is performed via three linear actuators. The residual gravity torque is not reported but the balancing system is able to reduce by 71% the standard

1.3. Review of CubeSat attitude testing facilities

deviation of the kinetic energy of the system (Chesi et al., 2014). The Air Force Institute of Technology (AFIT) testbed of the **Air Force Air University** consists of a Helmholtz cage and hemispherical air bearing with a tabletop ABT and allows testing 3U or larger CubeSats (Tibbs, 2015). Since the main objective was to test attitude control around the yaw axis, the payload is placed to ensure the ABT is level, while the vertical offset between CM and CR is not taken into consideration. The testbed is used to test a 6U CubeSat mock-up but it is reported that a 12U CubeSat could also be tested. The **University of Sydney** developed an attitude control test platform based on a spherical air bearing and a tabletop ABT designed to emulate the CubeSat ADCS (Kwan et al., 2015). RWs are used for actuation, an IMU for sensing the attitude. The mass balancing is manual and only the horizontal offset between CM and CR is compensated. An automatic mass balancing system has been developed, but no information about its implementation was found. The **University of Montpellier** and **CNRS** introduce a novel design for an air bearing testbed for CubeSat ADCS consisting of a robotic wrist which rotates air bearings in order to follow the CubeSat motion (Gavrilovich et al., 2015). The advantage of this new testbed consists in the fact that it allows virtually 3-DOF unlimited motion. The disadvantage consists of the limited size of the payload which can be tested. Mass balancing is manual, and it is performed by moving six small masses. A total disturbance torque of $2 \times 10^{-2} \text{ N m}$ is estimated (Gavrilovich, 2016). The testbed of the **University of Brasília** does not address CubeSat in particular but nanosatellites in general. It is composed of a Helmholtz cage for magnetic field simulation and of a spherical air bearing with a tabletop ABT (Silva et al., 2019). Linear actuators are used for automatic mass balancing, together with a microcontroller, an IMU and a communication module. The final step of the mass balancing is able to reduce the CM to CR offset vector norm to $289.42 \mu\text{m}$, with the vertical component being the largest, which combined with a total mass of 14 kg results in a gravity vector norm of $3.97 \times 10^{-2} \text{ N m}$. The **Virginia Polytechnic Institute and State University** developed the Virginia Tech CubeSat Attitude Control Simulator (CSACS): an air bearing testing platform composed of a Helmholtz cage and of a tabletop ABT (Wolosik, 2018). The mass balancing is performed with linear actuators, but it is tested only in simulation. The **Alma Mater Studiorum - Università di Bologna** (or **Unibo**) has developed a dynamic testbed for nanosatellites attitude verification. The testbed includes a Helmholtz cage, a Sun simulator, a ground-

truth system and a tabletop ABT and can host nanosatellites of size up to 6U, not necessarily compliant to the CubeSat standard (Modenini et al., 2020). The balancing procedure includes a coarse balancing using counterweight masses and an automatic finer balancing. Linear actuators, an IMU and a microcontroller are used to perform automatic fine balancing. The gravity torque is reduced to 5×10^{-5} N m when the platform has a total mass of 4.5 kg. This platform is used as the starting point for the new testbed development. **Sputnix** has developed a dynamic simulator for spacecraft ADCS test, which comprises a tabletop ABT, a magnetic field simulator, a Sun simulator, a ground-truth system and the possibility of hosting 1U, 3U and 6U CubeSats (Sputnix, 2020). The CM position is controlled with both load masses movable in the 3D space and with an automatic balancing system, employing six linear electromechanical actuators. The attitude is reconstructed using optical cameras. The CM position is set close to the CR position with accuracy of 1×10^{-5} m (Ivanov et al., 2021), which considering the total mass of 14.24 kg results in a residual gravity torque of 1.46×10^{-3} N m. The ADCS testbed realised by the **Norwegian University of Science and Technology (NTNU)** includes a Helmholtz cage, a spherical air bearing, a tabletop ABT and a Sun simulator. It is used to test a 2U CubeSat ADCS. Mass balancing is only verified qualitatively, by ensuring that the system is marginally unstable (Olsen, 2021). The **South Dakota State University** does not use an air bearing nor a robotic arm but rather a suspension mechanism made of high-tensile strength wires connected to a base for ADCS components (Lavezzi et al., 2022). The Suspended Satellite Three-Axis Rotation Testbed (START) testbed includes a set of RW for actuation and Optitrack cameras as ground truth system. The balancing system is manual and it is composed of sliding masses. The effectiveness of the balancing is assessed only qualitatively. **K. N. Toosi University of Technology** has developed a tabletop ABT testbed to test CubeSat ADCS. The mass balancing system consists of RW and linear actuators, the attitude is sensed using gyroscopes and accelerometers (Rivandi et al., 2023). The performance of the balancing system is not directly reported. The Nanosatellite Attitude Control Simulator (NACS) of the **McMaster University** consists of a spherical air bearing with a tabletop ABT, a 1U CubeSat mock-up and an automatic balancing system. Linear actuators and RWs are used as actuators during the balancing, while an IMU is used as sensor. Thanks to this system, the gravity torque norm is reduced to 4.84×10^{-5} N m (McCafferty-Leroux et al., 2024). The Space-

1.3. Review of CubeSat attitude testing facilities

craft Attitude Testbed (SAT) of the **San Diego State University** comprehends a Helmholtz cage, a spherical joint bearing for 3-DOF rotation, and a 1U CubeSat mock-up (Stromecki and Bani Younes, 2024). The mock-up ADCS includes RWs and MTQs as actuators and an IMU as sensor. The reported level of friction for the spherical joint was considered too high and it is planned that this is replaced with a spherical air bearing. The CM to CR offset is reduced to 1×10^{-4} m, considering the total mass of 3.3 kg, this results in a gravity torque of 3.24×10^{-3} N m. The **Berlin Space Technologies (BST)** NanoSat Air Bearing allows testing CubeSats of 1U and 3U form factor (GmbH, 2024). It can be integrated into the ADCS simulator, which consists of a spherical air bearing, a tabletop ABT, a magnetic field simulator, a Sun simulator and optionally a Global Navigation Satellite System (GNSS) simulator and a stars simulator (BST, 2024). The reported level of disturbance torque is 1×10^{-3} N m. The **Universidad Nacional de San Agustín de Arequipa (UNSA)** developed an air bearing testbed for nanosatellite ADCS with a ROS-based decentralised architecture ADCS module (Yanyachi et al., 2024). The testbed consists of a hollow hemisphere within which an ADCS module is placed. The ADCS module uses RWs for actuation and an IMU as sensor. Mass balancing is achieved solely by placing the module inside the air bearing cavity, resulting in a gravity torque of norm equal to 2.29×10^{-5} N m. The testbed developed at the **Universidad Católica San Pablo** consists of a spherical air bearing with a tabletop ABT. The testbed supports CubeSats up to 3U. The mass balancing is automatic and uses DC motors, which is reported to reduce cost and weight compared to systems which use linear actuators. The gravity torque norm is reduced to 3.94×10^{-3} N m (Contreras et al., 2025). **Clarkson University** presented a CubeSat Attitude Control Emulation Platform based on a three-axis gimbal and a Helmholtz cage. Differently from the other testbeds, here the torque produced by the CubeSat is estimated and the CubeSat attitude is mechanically varied following the kinematics which result from Euler equation integration. While this concept allows for more DOFs with respect to an air bearing, it has some limitations linked to the fact that the only torque estimated is the magnetic torque and a precise knowledge of the magnetic field is required. Furthermore, disturbance torques due to gravity-gradient and aerodynamic torques were not implemented (Wallshauser et al., 2025). A similar approach is used at the **Space Systems Department Zentrum fur Telematik (ZfT)**, where the attitude motion of the CubeSat under test is provided by a turntable based on simulation (Petermann et al.,

2025). It is not possible, however, to directly test the response of ADCS actuators (Ruf et al., 2024). Finally, **Astro-und Feinwerktechnik Adlershof GmbH (Astrofein)** developed a scaled version of their attitude control HIL test facility specifically designed to test CubeSat. It consists of an air bearing and a ABT, a Helmholtz cage, a Sun simulator, a ground-truth system, a GNSS simulator and a stars simulator to be interfaced with optical heads. It can host CubeSat from 3U to 16U in different orientations (Astrofein, 2025). The mass balancing consists of a manual calibration procedure with small sliding masses and a precise CubeSat positioning using adjustable sliders. This calibration procedure is reported to add a small moment of inertia to the test object while obtaining a level of gravity torque below $1 \times 10^{-5} \text{ N m}$ (Nicolai et al., 2025). This can be considered the most complete test facility with very low level of disturbance torques, being a commercial product, however, a precise description of the CubeSat positioning and CM calibration is lacking.

Apart from Table 1.2, Figure 1.5 provides a graphical comparison of facility performance in terms of: residual torque level, hosting and simulation capabilities. Only the facilities for which these pieces of information are available are shown.

Table 1.2: Existing CubeSat ADCS testing facilities. (Sim. = Simulation, Emu. = Emulation, Mag. = Magnetic)

Developer	Scope	Type	Sensors & actuators	DUT	Sim.	Mass balancing
MIT (2013)	Sim.	ABT, Tabletop		3U	Mag.	Not mentioned
York (2013)	Sim.	ABT, Tabletop	IMU	1U		Manual, $3.3 \times 10^{-2} \text{ N m}^*$
NPS (2015)	Emu.	ABT, Hollow	Sun sensor, IMU, metrology system, RW, MTQ, linear actuators		Mag., Sun	Automatic, gravity torque not estimated

1.3. Review of CubeSat attitude testing facilities

AFIT (2015)	Sim.	ABT, Hollow	Laser metrology system	3U, 6U, 12U	Mag.	Manual, only planar, gravity torque not estimated
Sydney (2015)	Emu.	ABT, Tabletop	IMU, RW			Manual, only planar gravity torque not estimated
Montpellier (2018)	Sim.	Air bearing & robotic wrist	Encoders	1U, 3U		Manual, $2 \times 10^{-2} \text{ N m}$
Brasilia (2018)	Sim.	ABT, Tabletop	IMU, linear actuators		Mag.	Automatic, $3.97 \times 10^{-3} \text{ N m}^*$
Virginia (2018)	Sim.	ABT, Tabletop	IMU, RW	At least 1U, 6U	Mag.	Automatic, only tested in simulation
Bologna (2020)	Sim.	ABT, Tabletop	Vision system, linear actuators	Up to 6U	Mag., Sun	Automatic, $5 \times 10^{-5} \text{ N m}$
Sputnix (2020)	Sim.	ABT, Tabletop	Vision system, linear actuators	1U, 3U, 6U	Mag., Sun	Automatic, $1.46 \times 10^{-3} \text{ N m}^*$
NTNU (2021)	Sim.	ABT, Tabletop		2U	Mag., Sun	Manual, qualitative check

South Dakota (2022)	Emu.	Suspended	Vision system, RW			Manual, qualitative check
K.N. Toosi (2022)	Sim.	ABT, Tabletop	IMU, RW, linear actuators	1U		Automatic, qualitative check
McMaster (2024)	Emu.	ABT, Tabletop	IMU, RW			Automatic, $4.84 \times 10^{-5} \text{ N m}^*$
San Diego (2024)	Emu.	Spherical joint bearing	IMU, RW, MTQ		Mag.	Automatic, $3.24 \times 10^{-3} \text{ N m}^*$
BST (2024)	Sim.	ABT, Tabletop		1U 3U	Mag., Sun, GNSS, Stars	Not specified $1 \times 10^{-3} \text{ N m}$
UNSA (2024)	Emu.	ABT, Hollow	IMU, RW			Manual, $2.29 \times 10^{-5} \text{ N m}$
San Pablo (2025)	Sim.	ABT, Tabletop	IMU	Up to 3U		Automatic, $3.94 \times 10^{-3} \text{ N m}$
Clarkson (2025)	Emu.	3-axes gimbal	MTQ	1U to 12U	Mag.	Not estimated
ZfT (2025)	Emu.	Turntable		1U to 16U	Sun, Stars	Not estimated
Astrofein (2025)	Sim.	ABT, Tabletop	Vision system	3U to 16U	Mag., Sun, GNSS, Stars	Automatic, $1 \times 10^{-5} \text{ N m}$
* Estimation based on the available information						

1.4. Highlight of existing gap in CubeSat ADCS testing facilities

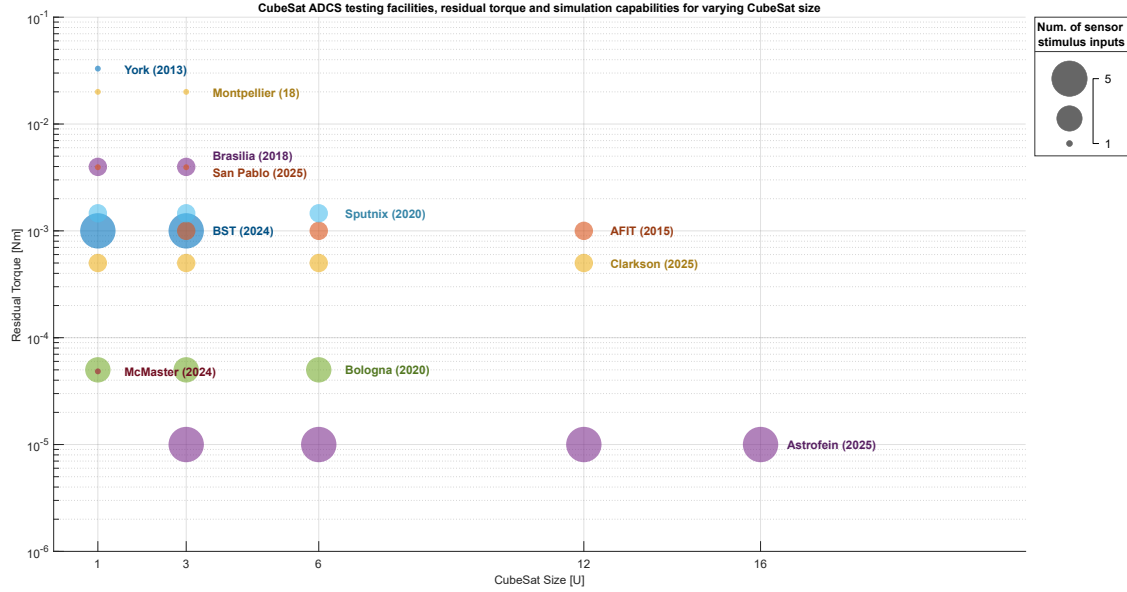


Figure 1.5: Attitude testing facility residual torque level at varying CubeSat size. The bubble size represents the number of sensor stimulus, with 1 being only the air bearing.

1.4 Highlight of existing gap in CubeSat ADCS testing facilities

From the literature study on existing attitude simulator testbed a few conclusions can be drawn:

- 9 out of 22 testbeds are emulation-only and therefore cannot test full CubeSat flight hardware in closed loop; system-level verification of the actual spacecraft ADCS is therefore not achievable on these platforms;
- most of the testbed present a level of gravity torque which is several orders of magnitude higher than the one a CubeSat experience in orbit. Just 4 testbeds out of 22 present level of gravity torque lower than 1×10^{-4} N m: University of Bologna (2020), McMaste University (2024), Universidad Nacional de San Agustín de Arequipa (2024), Astrofein (2025);
- Of these four, only two are simulation testbeds, with University of Bologna (2020) hosting CubeSat from 1U to 6U and Astrofein (2025) from 3U up to 16U;

- Both include magnetic-field, Sun simulators and a ground-truth metrology system. Astrofein (2025) offers also GNSS and stars simulation capabilities.

These considerations pose the need for a novel facility, which:

1. can host CubeSats of different sizes, ideally from 1U to 12U, with standard mounting mechanisms;
2. enables genuine system-level HIL testing;
3. provides representative environment stimulation (magnetic field, Sun and ideally stars);
4. offers an automatic CM calibration procedure for systematically obtaining residual gravity torque below $1 \times 10^{-4} \text{ N m}$

These characteristics would make a facility a distinctive solution for testing CubeSats ADCS truly as-you-fly.

While Astrofein (2025) can be considered a facility which satisfy these requirements, excluded the possibility of testing 1U CubeSats. Its development is very recent and can be considered a parallel development with respect to the one presented in this thesis. Moreover, Astrofein is a commercial product and technical details on centre-of-mass calibration, balancing strategy, and reconfigurability are not openly available.

The need for such a complete facility is also confirmed by a request for proposal by ESA, which explicitly identifies the lack of an end-to-end CubeSat ADCS verification and validation facility as a contributor to prolonged in-orbit commissioning campaigns due to ADCS issues.

2

Preliminaries

This chapter introduces the theoretical background for Part I of the thesis. Section 2.1 presents a description of the attitude representation, kinematics and dynamics of a rigid body in space. Section 2.2 describes the main disturbance torques acting on a satellite in space. An order of magnitude estimate of these torques is reported for different sizes of CubeSats. Section 2.4 presents the Cube-sat Space Protocol (CSP) and Space Packet Protocol (SPP) which are based on the Open System Interconnection (OSI) model, also described.

2.1 Attitude dynamics and kinematics

The fundamentals about the attitude dynamics of a rigid body are here recalled. Section 2.1.1 introduces the frame of reference used in this thesis. Section 2.1.2, Section 2.1.3 and Section 2.1.4 describe the attitude representation, kinematics and dynamics and are mainly based on [De Ruiter et al. \(2013\)](#), the description of environmental torque in Section 2.2 is also based on [De Ruiter et al. \(2013\)](#).

2.1.1 Frames of reference

The goal of a spacecraft ADCS is to estimate and control the relative orientation between two frames of reference, in which one of the two frames is fixed with

the spacecraft body. The frames of reference used in the present thesis work are here introduced. A distinction is made between frames of reference relative to a spacecraft travelling in a Earth orbit and frames of reference which are specific to the attitude simulation testbed.

For what concerns the reference frames which are relevant for a spacecraft in Earth orbit, we specify:

Earth-Centred Inertial (ECI)

It is a geocentric-equatorial inertial frame, its centre is coincident with the Earth's centre. Its z axis is perpendicular to the equatorial plane. The x axis points towards the vernal equinox. The y axis follows from the right-hand rule. It is indicated by $\mathcal{F}_{ECI}$. Since the origin of the system follows the Earth's revolution, it is not truly an inertial frame of reference. For practical purposes, however, can be considered to be inertial.

Earth-Centred-Earth-Fixed (ECEF)

It is a geocentric-equatorial non-inertial frame, its centre is coincident with the Earth's centre. Its z axis is perpendicular to the equatorial plane. The x axis points towards the prime meridian. The y axis follows from the right-hand rule. It is indicated as $\mathcal{F}_{ECEF}$. Since the axes orientation varies with Earth's rotation, it is not an inertial frame.

Radial-Transversal-Normal (RTN)

Its origin coincides with the spacecraft CM. The x axis direction goes from the centre of the Earth to the CM of the spacecraft. The z axis is aligned with the orbit angular momentum. The y axis follows from the right-hand rule. It is indicated as $\mathcal{F}_{RTN}$. It is not an inertial frame.

Body-fixed reference frame

The origin of this frame of reference is fixed with the body. It can be, for instance, coincident with its CM or its CR. The axes of the frame of reference are fixed with the spacecraft body, assumed to be rigid. This reference frame is denoted by $\mathcal{F}_b$.

For what concerns the reference frames which are relevant for the attitude simulator testbed, we introduce:

Laboratory reference frame The definition of this frame is strictly related to the air bearing facility layout. The centre of the frame is coincident with the Helmholtz cage centre. The z axis is coincident with the magnetic field generated by the z coil, positive upward. It is assumed that this direction coincides with

the local gravity direction. The x axis points to the Sun simulator rail. It is practically identified by the plane containing the magnetic field generated by the x and z coils and it is forced to be perpendicular to the z direction. The y axis follows from the right-hand rule. The frame is shown in Figure 2.1 for clarity. It is indicated with $\mathcal{F}_{LAB}$. It is considered to be an inertial system for simulation purposes.

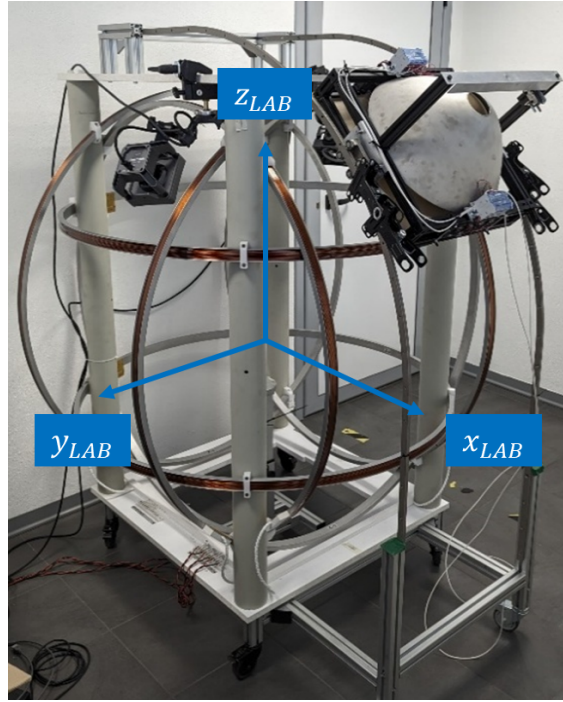


Figure 2.1: Laboratory frame definition

Air bearing table platform reference frame The origin of the reference system is the CR of the air bearing platform. The x and y axes lie in the platform plane and are dependent on the specific platform, the z axis is perpendicular to the platform plane. Specifically, on the 1U platform, the x axis goes from the battery pack to the opposite platform side, the y axis goes from the vertical stepper motor side to the opposite side, the z axis follows from the right-hand rule. On the 3U-12U platform, the x axis goes from the horizontal stepper motor side to the vertical stepper motor side, the y axis goes from the voltage regulator side to the IMU side, the z axis follows from the right-hand rule. The frame definition is shown in Figure 2.2 for clarity.

The platform reference frame is indicated as $\mathcal{F}_P$. It is not an inertial frame.

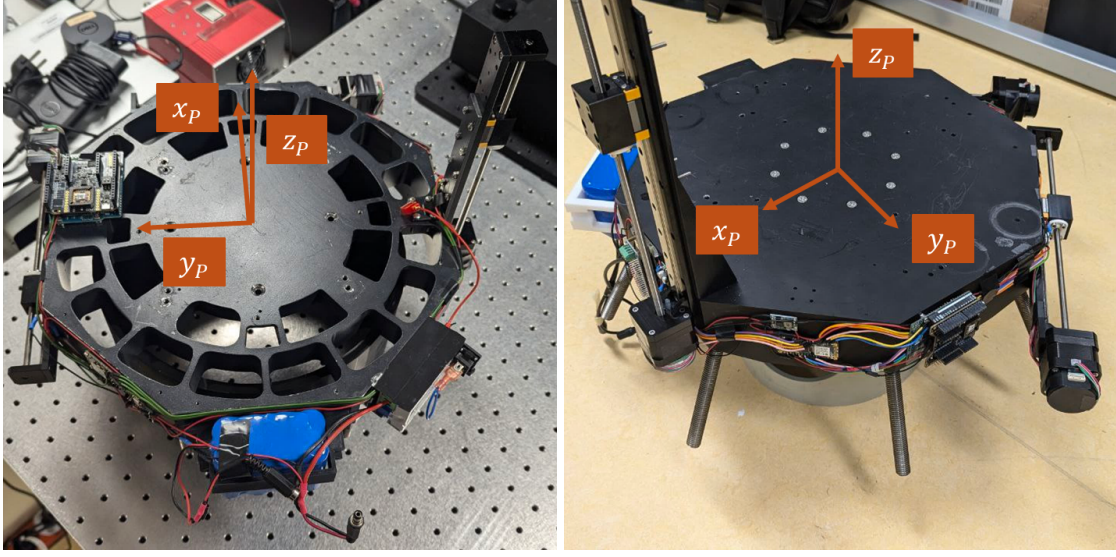


Figure 2.2: Air bearing table platform frame definition for the 1U platform (left) and for the 3U-12U platform (right)

2.1.2 Attitude representation

There are different possibilities to represent the orientation of a reference frame with respect to another. In this work, mainly three types of attitude representations are used: rotation matrix, Euler angles and unit quaternions. In the thesis work, passive rotations are considered, where the same physical vector is represented in rotated frames of reference, rather than active rotations, where the vector rotates.

Rotation matrix

Given two reference frames, $\mathcal{F}_1$ and $\mathcal{F}_2$, identified by the unit vectors $\mathcal{F}_1 = [\mathbf{x}_1^T \mathbf{y}_1^T \mathbf{z}_1^T]^T$ and $\mathcal{F}_2 = [\mathbf{x}_2^T \mathbf{y}_2^T \mathbf{z}_2^T]^T$, and an arbitrary vector $\mathbf{v}$, this can be expressed in the two reference frames as:

$$\mathbf{v} = \mathcal{F}_1 \mathbf{v}_1 = \mathcal{F}_2 \mathbf{v}_2 \quad (2.1)$$

where $\mathbf{v}_1$ and $\mathbf{v}_2$ are the coordinate vectors of $\mathbf{v}$ in the two reference frames. From the previous equation, we can write:

$$\mathbf{v}_2 = \mathcal{F}_2^T \mathcal{F}_1 \mathbf{v}_1 = R_1^2 \mathbf{v}_1 \quad (2.2)$$

where R_1^2 is called the rotation matrix and describes the relative orientation be-

tween the two reference frames:

$$R_1^2 = \mathcal{F}_2^T \mathcal{F}_1 = \begin{bmatrix} \mathbf{x}_2^T \\ \mathbf{y}_2^T \\ \mathbf{z}_2^T \end{bmatrix} [\mathbf{x}_1 \ \mathbf{y}_1 \ \mathbf{z}_1] = \begin{bmatrix} \mathbf{x}_2^T \mathbf{x}_1 & \mathbf{x}_2^T \mathbf{y}_1 & \mathbf{x}_2^T \mathbf{z}_1 \\ \mathbf{y}_2^T \mathbf{x}_1 & \mathbf{y}_2^T \mathbf{y}_1 & \mathbf{y}_2^T \mathbf{z}_1 \\ \mathbf{z}_2^T \mathbf{x}_1 & \mathbf{z}_2^T \mathbf{y}_1 & \mathbf{z}_2^T \mathbf{z}_1 \end{bmatrix} \quad (2.3)$$

The matrix R_1^2 is also called director cosine matrix since each entry is the scalar product between the unit vectors of the reference frame basis, which is equal to the cosine of the angle between them.

The rotation matrix satisfies a number of properties: the transpose of a rotation matrix is equal to its inverse, making it an orthogonal matrix, the product of the inverse of a rotation matrix times itself results in the identity matrix, making it an orthonormal matrix. Subsequent rotations can be easily concatenated, rotation from frame 1 to frame 3 can be expressed as the matrix product of rotation from 2 to 3 and rotation from 1 to 2:

$$R_1^3 = R_2^3 R_1^2 \quad (2.4)$$

Euler angles

Rotations around coordinate axes are called principal rotations. The Euler angles parametrise a set of three consecutive principal rotations. In this work, the 3-2-1 sequence of rotations is used:

1. a rotation $R_z(\psi)$ about the original z -axis, called *yaw*;
2. a rotation $R_y(\theta)$ about the intermediate y -axis, called *pitch*;
3. a rotation $R_x(\phi)$ about the transformed x -axis, called *roll*.

The corresponding rotation matrix from frame F_1 to frame F_2 is given by:

$$\begin{aligned} R_1^2(\phi, \theta, \psi) &= R_x(\phi) R_y(\theta) R_z(\psi) = \\ &= \begin{bmatrix} 1 & 0 & 0 \\ 0 & c(\phi) & s(\phi) \\ 0 & -s(\phi) & c(\phi) \end{bmatrix} \begin{bmatrix} c(\theta) & 0 & -s(\theta) \\ 0 & 1 & 0 \\ s(\theta) & 0 & c(\theta) \end{bmatrix} \begin{bmatrix} c(\psi) & s(\psi) & 0 \\ -s(\psi) & c(\psi) & 0 \\ 0 & 0 & 1 \end{bmatrix} \end{aligned}$$

$$= \begin{bmatrix} c(\theta)c(\psi) & c(\theta)s(\psi) & -s(\theta) \\ s(\phi)s(\theta)c(\psi) - c(\phi)s(\psi) & s(\phi)s(\theta)s(\psi) + c(\phi)c(\psi) & s(\phi)c(\theta) \\ c(\phi)s(\theta)c(\psi) + s(\phi)s(\psi) & c(\phi)s(\theta)s(\psi) - s(\phi)c(\psi) & c(\phi)c(\theta) \end{bmatrix} \quad (2.5)$$

where c and s are shorthand for $\cos$ and $\sin$.

Unit quaternions

A consequence of Euler's theorem is that any rotation between two frames can be parametrised by a rotation about a principal axis $\mathbf{a}$ and an angle ϕ . Using these parameters, we can define unit quaternions as:

$$q_0 = \cos\left(\frac{\phi}{2}\right) \quad (2.6)$$

$$\mathbf{q}_e = \mathbf{a} \sin\left(\frac{\phi}{2}\right) \quad (2.7)$$

where q_0 is the scalar part of the quaternions and $\mathbf{q}_e = [q_1 \ q_2 \ q_3]^T$ is the vector part of the quaternion. The unit quaternion satisfies the following constraint:

$$q_0^2 + q_1^2 + q_2^2 + q_3^2 = 1 \quad (2.8)$$

In this work the "scalar-first" convention is used for the quaternion:

$$\mathbf{q} = [q_0 \ q_1 \ q_2 \ q_3]^T \quad (2.9)$$

The relationship between quaternions and rotation matrices is given by:

$$R(\mathbf{q}) = \begin{bmatrix} 1 - 2(q_2^2 + q_3^2) & 2(q_1q_2 + q_3q_0) & 2(q_1q_3 - q_2q_0) \\ 2(q_1q_2 - q_3q_0) & 1 - 2(q_1^2 + q_3^2) & 2(q_2q_3 + q_1q_0) \\ 2(q_1q_3 + q_2q_0) & 2(q_2q_3 - q_1q_0) & 1 - 2(q_1^2 + q_2^2) \end{bmatrix} \quad (2.10)$$

2.1.3 Attitude kinematics

The temporal evolution of the attitude representation of a rigid body can be linked to its angular velocity. The description of the link is the attitude kinematics. Let's call $\boldsymbol{\omega} = [\omega_x \ \omega_y \ \omega_z]^T$ the angular velocity of a frame 2 with respect to a frame 1, whose coordinates are given in frame 2. It can be shown that the temporal evolution of a quaternion representation transforming coordinates from

frame 1 to frame 2 is described by:

$$\dot{\mathbf{q}}_e = \frac{1}{2}q_0\boldsymbol{\omega} + \frac{1}{2}[\mathbf{q}_e^\times]\boldsymbol{\omega} \quad (2.11)$$

$$\dot{q}_0 = -\frac{1}{2}\mathbf{q}_e^T\boldsymbol{\omega} \quad (2.12)$$

2.1.4 Attitude dynamics

Let's consider the angular velocity $\boldsymbol{\omega}$ of a frame attached to a body with inertia matrix J . The angular momentum of the body in body axes is given by:

$$\mathbf{L} = J\boldsymbol{\omega} \quad (2.13)$$

A torque acting on the body and described in the body frame is denoted by $\boldsymbol{\tau}$. The Euler's equation of motion describes the temporal evolution of the body angular momentum as a function of external torques. The Euler's equation for a rotation around any CR is given by:

$$\dot{\mathbf{L}} = -\boldsymbol{\omega} \times \mathbf{L} + \boldsymbol{\tau} + \mathbf{r}_{CM} \times m_{tot}\mathbf{a}_{CM} \quad (2.14)$$

where $\mathbf{r}_{CM}$ is the position vector of the CM with respect to the CR, $\mathbf{a}_{CM}$ is the acceleration of the CM in body frame. In the case the CR and CM coincide, the equation simplifies to:

$$\dot{\mathbf{L}} = -\boldsymbol{\omega} \times \mathbf{L} + \boldsymbol{\tau} \quad (2.15)$$

and the angular momentum $\mathbf{L}$ is about the CM.

2.2 The environmental disturbance torques

A spacecraft in orbit is subject to different sources of disturbance torques. The torques' magnitude can be very low, especially for small satellites. Actuators on the other side are also limited in size and in the level of control torque they can generate. Testing of the ADCS in a relevant environment requires ideally that the disturbance torques acting on the satellite under test are of the same order of magnitude of the torque present in space. In practice, slightly larger torques

still allow to perform functional testing. A characterisation of the environmental disturbance torques acting on a CubeSat in space is reported here. Section 2.2.1 to Section 2.2.4 describe briefly the disturbance torques acting in LEO. In Section 2.2.5 the order of magnitude of these torques is calculated for different sizes of CubeSats.

2.2.1 Gravity gradient torque

The gravitational force which acts on a mass in space is inversely proportional to the square of the distance from the Earth's centre. Intuitively, the portions of mass of a rigid body closer to Earth experience a greater gravitational force than the portions further from it, causing a torque. This torque due to the gravity gradient can be modelled for a continuous body as the integral of torques around the CM acting on an infinitesimal mass element dm . By making the assumption that the spacecraft size is much smaller than the distance from the Earth's centre (certainly valid for a CubeSat), an expression for the gravity gradient torque in the body frame can be obtained:

$$\boldsymbol{\tau}_{GG} = \frac{3\mu}{r^3} \hat{\mathbf{r}} \times (J\hat{\mathbf{r}}) \quad (2.16)$$

where μ is the Earth standard gravitational parameter, $\mathbf{r}$ is the spacecraft position vector with respect to the Earth's centre expressed in body frame, J is the inertia matrix of the spacecraft. The gravity gradient torque decreases with the cube of the distance from the Earth's centre, so it is relevant in LEO. It is equal to zero for a spherical object and when $\mathbf{r}$ is parallel to a principal axis of inertia.

2.2.2 Magnetic torque

A magnetic dipole in an external magnetic field tends to align with it, creating a torque. Current loops caused by electronic components on board spacecrafts create a dipole that interacts with the Earth's magnetic field, producing a magnetic torque:

$$\boldsymbol{\tau}_M = \mathbf{m} \times \mathbf{B} \quad (2.17)$$

where $\mathbf{m}$ is the magnetic dipole, $\mathbf{B}$ is the magnetic field.

The same principle is used on board spacecrafts to create a control torque using magnetic actuators, like magnetorquers.

2.2.3 Aerodynamic torque

Despite the very low atmospheric density in LEO spacecrafts encounter aerodynamic resistance. This is caused by the exchange of momentum between the atmosphere molecules and the spacecraft, which travels at high speed relative to the atmosphere. The total aerodynamic force acting on the spacecraft can be calculated as:

$$\mathbf{F}_a = -\rho_a v^2 \hat{\mathbf{v}} \int_{S_{wa}} (\hat{\mathbf{n}} \cdot \hat{\mathbf{v}}) dS \quad (2.18)$$

where ρ_a is the atmospheric density, $\mathbf{v}$ is the spacecraft velocity, $\hat{\mathbf{n}}$ is the normal to the infinitesimal surface dS and S_{wa} is the wetted area, i.e. the area facing the flow for $\hat{\mathbf{n}} \cdot \hat{\mathbf{v}} > 0$.

By defining the centre of aerodynamic pressure $\mathbf{c}_{PA}$:

$$\mathbf{c}_{PA} = \frac{\int_{S_{wa}} \mathbf{r}(\hat{\mathbf{n}} \cdot \hat{\mathbf{v}}) dS}{\int_{S_{wa}} (\hat{\mathbf{n}} \cdot \hat{\mathbf{v}}) dS} \quad (2.19)$$

where $\mathbf{r}$ is the position vector of dS relative to the spacecraft CM, the aerodynamic torque is given by:

$$\boldsymbol{\tau}_a = \mathbf{c}_{PA} \times \mathbf{F}_a \quad (2.20)$$

The aerodynamic torque is equal to zero when the centre of pressure is coincident with the CM or when the total aerodynamic force is parallel to the line connecting the centre of pressure and CM.

Often, to evaluate the aerodynamic force, a slightly different equation is used, which resembles more the aerodynamic force formula used in atmospheric flight dynamics:

$$\mathbf{F}_a = -\frac{1}{2} \rho C_d S_{wa} v^2 \hat{\mathbf{v}} \quad (2.21)$$

where C_d is the drag coefficient, usually in between 2.0 and 2.5 for a spacecraft (Wertz et al., 2011).

2.2.4 Solar radiation pressure torque

The exchange of momentum between the solar radiation and the spacecraft surface creates a force. There are different ways of interactions which can be modelled as: total absorption, where all the momentum is transferred to the surface, specular reflection, where all the momentum is reflected in a specular direction with respect to the impinging radiation and diffuse reflection, where it is reflected in a diffuse way around the surface normal. The type of interaction can be, in general, very complex. For preliminary sizing purposes, total absorption can be assumed; in this case, the solar radiation pressure force can be calculated as:

$$\mathbf{F}_S = -\frac{\Phi}{c} \int_{S_{wa}} (\hat{\mathbf{n}} \cdot \hat{\mathbf{s}}) dS \quad (2.22)$$

where $\hat{\mathbf{s}}$ is the sun direction, $\hat{\mathbf{n}}$ is the normal to the infinitesimal surface dS , S_{wa} is the wetted area and $\Phi = 1366 \text{ W/m}^2$ is the radiation flux intensity at Earth distance from the sun, c is the speed of light.

Similarly to the aerodynamic torque, the solar radiation pressure torque is given by:

$$\boldsymbol{\tau}_s = \mathbf{c}_{PS} \times \mathbf{F}_S \quad (2.23)$$

where $\mathbf{c}_{PS}$ is the centre of solar pressure:

$$\mathbf{c}_{PS} = \frac{\int_{S_{wa}} \mathbf{r}(\hat{\mathbf{n}} \cdot \hat{\mathbf{s}}) dS}{\int_{S_{wa}} (\hat{\mathbf{n}} \cdot \hat{\mathbf{s}}) dS} \quad (2.24)$$

2.2.5 Disturbance torque for a CubeSat in Low Earth Orbit

Given the model of typical disturbance torques in LEO, an order of magnitude estimate for the disturbance torques acting on a CubeSat can be made. This can then be used as a reference for the facility performance in terms of reduction of disturbance torques. The estimate is made for CubeSats of size 1U, 3U, 6U and 12U. A typical LEO Sun-Synchronous orbit is considered, the orbital characteristics are reported in Table 2.1.

To estimate the gravity gradient torque, the maximum value assumed by Equation (2.16) is employed:

Table 2.1: Orbital characteristics for the reference LEO mission

Name	Symbol	Value
Epoch	T_0	1 st of January 2025 12:00 UTC
Altitude	H	500 km
Eccentricity	e	0°
Right Ascension of the ascending node	Ω	192.3°
Inclination	i	97.4°

Table 2.2: CubeSat mass, sizes and principal moments of inertia

CubeSat	Mass kg	Size $x \times y \times z$ [mm]	Principal moments of inertia [kg m ²]
1U	2	100.0 × 100.0 × 113.5	$J_{xx} = 0.0038$ $J_{yy} = 0.0038$ $J_{zz} = 0.0038$
3U	6	100.0 × 100.0 × 340.5	$J_{xx} = 0.0630$ $J_{yy} = 0.0630$ $J_{zz} = 0.0100$
6U	12	226.0 × 100.0 × 366	$J_{xx} = 0.1440$ $J_{yy} = 0.1850$ $J_{zz} = 0.0611$
12U	24	226.3 × 226.3 × 366	$J_{xx} = 0.3701$ $J_{yy} = 0.3701$ $J_{zz} = 0.2043$

$$\tau_{gg} = \frac{3}{2} \frac{\mu}{r^3} (J_{Max} - J_{min}) \quad (2.25)$$

where J_{Max} is the largest moment of inertia and J_{min} is the smallest one.

The magnetic torque is evaluated as:

$$\tau_M = mB \quad (2.26)$$

where B is calculated using the approximate dipole formula and is equal to $4.96 \times 10^{-5} T$. The residual dipole torque is estimated using NASA standard SP-8018 Class III mass-based formula; 0.01 Am²/kg as recommended in [Cantiello](#)

et al. (2022).

The aerodynamic torque is calculated using the following formula:

$$\tau_a = \frac{1}{2} \rho C_D S_{wa} v^2 c_{pa} \quad (2.27)$$

The atmospheric density value is set to $1.7 \times 10^{-11} \text{ kg m}^3$ (Braeunig, 2025), the wetted surface is set equal to the largest spacecraft surface, c_{pa} distance is set equal to the max allowable distance between the CM and geometrical centre of a CubeSat over the longest side(see Table 1.1), C_D is set equal to 2.

The solar radiation pressure torque is calculated as:

$$\tau_{SRP} = \frac{\Phi}{c} S_{wa} c_{ps} \quad (2.28)$$

The centre of solar pressure distance from the CM is set equal to the centre of aerodynamic pressure distance from the CM.

Table 2.3 reports the result of the estimation of disturbance torques for the different sizes of CubeSats.

Table 2.3: Environmental disturbance torque calculations for different sizes of CubeSats

Disturbance Torque	1U	3U	6U	12U
Gravity gradient [Nm]	1.77×10^{-9}	1.95×10^{-7}	4.56×10^{-7}	6.09×10^{-7}
Magnetic [Nm]	9.91×10^{-7}	2.97×10^{-6}	5.95×10^{-6}	1.19×10^{-5}
Aerodynamic [Nm]	2.24×10^{-7}	1.51×10^{-6}	5.70×10^{-6}	5.70×10^{-6}
Solar radiation pressure [Nm]	1.03×10^{-9}	6.98×10^{-9}	2.64×10^{-8}	2.64×10^{-8}
Torque sum [Nm]	1.22×10^{-6}	4.69×10^{-6}	1.21×10^{-5}	1.82×10^{-5}

As expected for a small satellite LEO mission, the magnetic torque and the aerodynamic torque are the predominant ones. The level of disturbance torques is in line with the one founds by other works, as for instance Gavrilovich (2016).

2.3 Model of a rotating platform with integrated balancing system

The mathematical framework at the basis of the testbed balancing system is here introduced. The rotating platform is modelled as a rigid body employed with three orthogonal sliding point masses. Two reference frames are introduced, both with origin at the CR of the air bearing:

- the laboratory frame $\mathcal{F}_i = \{x_i, y_i, z_i\}$, assumed to be inertial, has its z axis aligned with the local vertical, while the x and y axes lie in the plane perpendicular to it, also called horizontal plane;
- the balancing frame $\mathcal{F}_b = \{x_b, y_b, z_b\}$, has its axes aligned with the three orthogonal sliding masses moving direction. This frame rotates with the platform.

The two reference frames are shown in Figure 2.3.

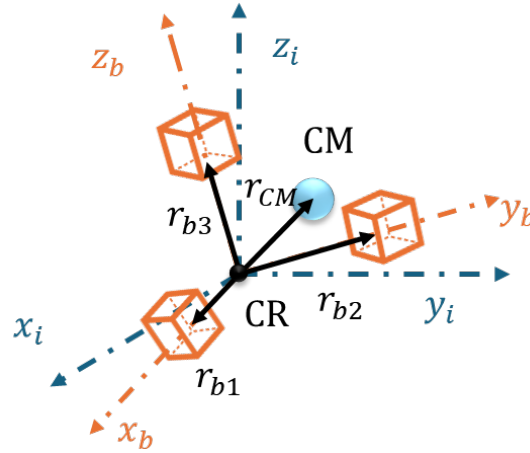


Figure 2.3: Mass balancing reference frames and CM position vector

The scope of the balancing system is to reduce the relative position between the CM and CR. It is hence useful to compute what is the effect of changing the relative position of the three sliding masses on the CM position. Recall that for a set of N point masses, the CM position can be calculated as:

$$\mathbf{r}_{CM} = \frac{\sum_{i=1}^N m_i \mathbf{r}_i}{\sum_{i=1}^N m_i} = \frac{\sum_{i=1}^N m_i \mathbf{r}_i}{m_{tot}} \quad (2.29)$$

where $\mathbf{r}_{CM}$ is the position vector of the CM, m_i is the mass value of the i point mass and $\mathbf{r}_i$ its position vector, m_{tot} is the rotating platform total mass. If we specify Equation (2.29) for the case in which the system is composed of three sliding masses m_{b1} , m_{b2} , m_{b3} and of a fourth point mass, with mass equal to $m_0 = m_{tot} - m_{b1} - m_{b2} - m_{b3}$ and position vector $\mathbf{r}_0$, we can write Equation (2.29) twice to evaluate what is the effect of moving the three sliding masses position between configuration I and II while keeping $\mathbf{r}_0$ the same:

$$\mathbf{r}_{CM,I} = \frac{1}{m_{tot}}(m_0\mathbf{r}_0 + m_{b1}\mathbf{r}_{1,I} + m_{b2}\mathbf{r}_{2,I} + m_{b3}\mathbf{r}_{3,I}) \quad (2.30)$$

$$\mathbf{r}_{CM,II} = \frac{1}{m_{tot}}(m_0\mathbf{r}_0 + m_{b1}\mathbf{r}_{1,II} + m_{b2}\mathbf{r}_{2,II} + m_{b3}\mathbf{r}_{3,II}) \quad (2.31)$$

The CM shift caused by the variation in position of the three sliding masses can be hence calculated as:

$$\Delta\mathbf{r}_{CM} = \frac{1}{m_{tot}}(m_{b1}\Delta\mathbf{r}_1 + m_{b2}\Delta\mathbf{r}_2 + m_{b3}\Delta\mathbf{r}_3) \quad (2.32)$$

where $\Delta\mathbf{r}_i = \mathbf{r}_{i,II} - \mathbf{r}_{i,I}$, $i = 1, 2, 3$. If we assume that the three sliding masses have an equal value $m_{b1} = m_{b2} = m_{b3} = m_b$, then Equation (2.32) becomes:

$$\Delta\mathbf{r}_{CM} = \frac{m_b}{m_{tot}}\Delta\mathbf{r} \quad (2.33)$$

where $\Delta\mathbf{r} = \Delta\mathbf{r}_1 + \Delta\mathbf{r}_2 + \Delta\mathbf{r}_3$.

Equation (2.33) shows that:

- the effect of moving three orthogonal masses of equal value in three orthogonal directions is the same of moving a single mass in a direction determined by the sum of the three orthogonal displacement vectors;
- the resolution in controlling the CM position depends on the resolution with which the sliding mass can be moved;
- the resolution in controlling the CM position depends on the ratio between the sliding mass value and the total mass value;

This last fact determines, in turn, the minimum theoretically achievable gravity torque norm $\tau_{g,min}$ which can be expressed as:

$$\tau_{g,min} = m_{tot}\Delta r_{CM,min}g = m_b\Delta r_{min}g \quad (2.34)$$

where $\Delta r_{CM,min}$ is the minimum amount the CM position can be changed, which depends on Δr_{min} , minimum amount a sliding mass position can be changed. Note that the minimum gravity torque norm does not depend on the total mass but just on the sliding mass value and the sliding mass change in position resolution.

To achieve small values of the gravity torque norm, the balancing system should employ small masses which can be moved with high resolution. At the same time, Equation (2.33) determines the control authority in changing the CM position. Clearly the two requirements are in conflict. This justifies the design of the balancing system as composed of a coarse and fine balancing part, described in Section 3.4.1.

The rotating platform inertia about the CR can be written as the sum of the moment of inertia of the platform without the balancing sliding masses J_0 and the contribution given by the sliding masses:

$$J = J_0 + \sum_{i=1}^3 (-m_{bi}[\mathbf{r}_i \times][\mathbf{r}_i \times]) \quad (2.35)$$

where $[\mathbf{r}_i \times]$ is the cross product matrix of the i sliding mass position vector.

The rotating platform angular momentum variation about the CR can then be written as:

$$\dot{\mathbf{L}} = \frac{d}{dt}(J\boldsymbol{\omega}) = J\dot{\boldsymbol{\omega}} + \frac{d}{dt} \left(\sum_{i=1}^3 (-m_{bi}[\mathbf{r}_i \times][\mathbf{r}_i \times]) \right) \boldsymbol{\omega} \approx J\dot{\boldsymbol{\omega}} \quad (2.36)$$

where it is assumed that the temporal variation of the inertia due to the sliding masses' movement is negligible with respect to the variation of inertia due to the platform change in angular speed.

The Euler equations specified about the CR is:

$$\dot{\mathbf{L}} = -\boldsymbol{\omega} \times \mathbf{L} + \boldsymbol{\tau} + \mathbf{r}_{CM} \times m_{tot}\mathbf{g}^b \quad (2.37)$$

where $\mathbf{g}^b$ is the gravitational acceleration vector in body axis, and it is assumed to be the only linear acceleration acting on the rotating platform. Note that the

last term in Equation (2.37) would not be present if the equation was written around the CM. This term can be further split into the sum of the position of the CM relative to the CR at a given instant $\mathbf{r}_{CM,0}$ plus the shift in CM position at a subsequent instant $\Delta\mathbf{r}_{CM}$ due to the change in sliding masses position $\Delta\mathbf{r}$, which is given by Equation (2.33). Equation (2.37) thus becomes:

$$\dot{\mathbf{L}} = -\boldsymbol{\omega} \times \mathbf{L} + \mathbf{r}_{CM,0} \times m_{tot}\mathbf{g}^b + \Delta\mathbf{r} \times m_{tot}\mathbf{g}^b \quad (2.38)$$

the term $\Delta\mathbf{r} \times m_{tot}\mathbf{g}^b = \boldsymbol{\tau}_u$ can be effectively seen as a control torque for the rotating platform dynamics. Note that it is assumed that this is the only control torque available, since no other form of actuation will be present. Combining Equation (2.13) and Equation (2.38) we obtain:

$$J\dot{\boldsymbol{\omega}} = -\boldsymbol{\omega} \times J\boldsymbol{\omega} + \mathbf{r}_{CM,0} \times m_{tot}\mathbf{g}^b + \boldsymbol{\tau}_u \quad (2.39)$$

$$\boldsymbol{\tau}_u = \Delta\mathbf{r} \times m_{tot}\mathbf{g}^b \quad (2.40)$$

The goal of the balancing system is to design a $\boldsymbol{\tau}_u$ which allows to minimise the CM to CR offset. However, not every control torque $\boldsymbol{\tau}_u$ can be actuated by the balancing system, Equation (2.40) clearly shows that the control torque will always lie in the plane perpendicular to the gravity vector. The control torque shall then be designed in such a way it can be actuated by the balancing system, this is described in Section 3.4.

2.4 Space communication protocols

Satellites in space use the link with a ground station to send Telemetry (TM) to the ground and receive Telecommand (TC). Moreover, information is exchanged between the different spacecraft subsystems/components and it is handled by the On-Board Data Handling (OBDH) subsystem. Specific communication protocols have been defined for space links by international entities such as the Consultative Committee for Space Data Systems (CCSDS) or by the small satellite community, as in the case of the Cubesat Space Protocol (CSP).

The CubeDynA facility communication module supports two space protocols for communication between the Device Under Test (DUT) and the ground station:

the CSP (via Controller Area Network (CAN) and WiFi) and the Space Packet Protocol (SPP) (via Inter-Integrated Circuit (I2C) and WiFi). Both protocols can be schematised using the style established by the Open System Interconnection (OSI) Basic Reference Model. The CSP protocol in particular is defined on the TCP/IP model, which is antecedent to the OSI model. The latter is more comprehensive and the two can be put in relation. In particular, the TCP/IP does not distinguish the top 3 layers of the OSI model. The OSI model is described since it provides a common framework for the development of standards in the field of systems interconnection (CCSDS, 2023).

A general overview of the OSI model, of the CSP and SPP is given respectively in Section 2.4.1, Section 2.4.2 and Section 2.4.3.

2.4.1 The Open System Interconnection model

The Reference Model of the OSI provides a common basis for the coordination of standards development for the purpose of systems interconnection. It is defined in the ISO/IEC 7498-1 standard, prepared by the International Organization for Standardization (ISO) and International Electrotechnical Commission (IEC) joint commission (ISO, 1994).

The standard introduces the concept of a layered architecture and of open system. An open system is defined as a system compliant with the OSI standard in its communication with other open systems. Each open system is made of an ordered set of N subsystems, organised in layers. Entities in the same layer can communicate between each other, with the communication being governed by protocols. The $N+1$ layer inherits the services from the lower N layer, in a bottom-up manner. Entities from the same layer can communicate only by using the services of the lower layer. The standard defines different modes of communication: a connection-mode service and a connectionless-mode service. Both modes require the definition of associations between the entities, such as addresses and type of protocol. The flow of information within layers of a transmitting device is top-down, while the one on a receiving device is bottom-up. On the transmitting device, the data to be transmitted is formed at the highest layer of the transmitting device into a Protocol Data Unit (PDU) and passed to the lower layer, where it is defined as the Service Data Unit (SDU). The SDU is concatenated with a header and/or footer, producing a new PDU which is passed to

the lower layer. The process continues down to the lowermost layer from which it is transmitted to the receiving device. On the receiving device, the data passes from the bottommost layer to the topmost layer as a series of SDUs, at each layer, the header and footer are processed and removed (header/footer stripping).

The reference model contains seven layers:

- the **Application Layer (Layer 7)** is the highest layer in the reference model, it provides the mean for an application (such as a software application) to access the Open System Interconnection Environment (OSIE). Its function is to provide communication with systems that are not part of the lower layers;
- the **Presentation Layer (Layer 6)**: it provides for common representation of the data transferred between different applications entities. Its functions include the definition and management of the syntax for the different application entities;
- the **Session Layer (Layer 5)** is the layer which makes it possible for the presentation entities to organise and synchronise their communication and manage data exchange. In connection-mode, it provides services for establishing a connection between two presentation entities, to support data exchange interaction and to end the connection in an orderly manner. In connectionless-mode, it provide services for the transmission and for exception reporting;
- the **Transport Layer (Layer 4)** provides the transmission of data between the session entities, taking into consideration reliability and cost effectiveness. It allows to obtain the performance required by the session entities at minimum cost, optimising the use of the available network services. In connection-mode, it establishes and end the transport connection and provides data transfer. Its function include addressing, sequence control, segmentation and reassembly of data. In connectionless-mode segmentation and reassembly are not provided;
- the **Network Layer (Layer 3)** provides the means for the transport entities for data transmission in connection-mode or connectionless-mode. It provides network addresses to the transport entities, network connection

parameters, quality of service parameters and error notification, among others. The functions of the network layer include routing and relaying, network connection, error management, flow control, mapping between network addresses and data link addresses;

- the **Data Link Layer (Layer 2)** provides the means for establishing, maintaining and releasing data-link between network entities and for the transfer of data units. A data link connection is built over one or more physical connections. The data link layer detects and possibly corrects errors which may occur in the physical layer. Additionally, it enables the network layer to control the interconnection of data circuits within the physical layer. Its functions includes the mapping between data link services and data units and the control of data circuit interconnection; additionally, in connection-mode, it also includes data link connection establishment and release, sequence and flow control and synchronisation;
- the **Physical Layer (layer 1)** provides the physical means (mechanical, electrical) and procedural means to manage physical connections for bit transmission between data link entities. Its functions include the physical connection activation and deactivation, the physical service data unit transmission, the multiplexing and the physical layer management. In general, the functions of the physical layer depend on the underlying physical medium.

A formal and detailed description of the OSI model can be found in the official ISO/IEC 7498-1 standard (ISO, 1994).

2.4.2 The Cubesat Space Protocol

The Cubesat Space Protocol (CSP) is a small protocol stack written in C, designed to ease communication between distributed embedded systems in small networks, such as CubeSats. The idea was developed by a group of students from Aalborg University in 2008 (GomSpace, 2011) and it is now maintained by Space Inventor and SpaeCubics, with contributions in the past from GomSpace and NTNU (libcsp authors, 2023b). The design follows the TCP/IP model and includes a transport protocol, a routing protocol and several MAC-layer interfaces.

One of the core strengths of the protocol is the lightweight header, which allows a 8-bit system to be fully connected to the network (libcsp authors, 2023a). The protocol stack includes functionalities on all layers of the TCP/IP model. At layer 1, drivers for some specific platforms have been included, while for unsupported platforms the user should add a new driver. At layer 2, CSP provides many interfaces, including interfaces for I2C and CAN. At layer 3, the CSP includes a router, which forwards the packets based on their CSP header. At layer 4, CSP implements two different transport layer protocols, called UDP (Unreliable Datagram Protocol) and RDP (reliable datagram protocol). The name UDP has not been chosen to be an exact replica of the User Datagram Protocol (UDP) known from the TCP/IP model, but they have certain similarities. The protocol is based on a 32-bit header containing both network and transport layer information. The header is shown in Figure 2.4.

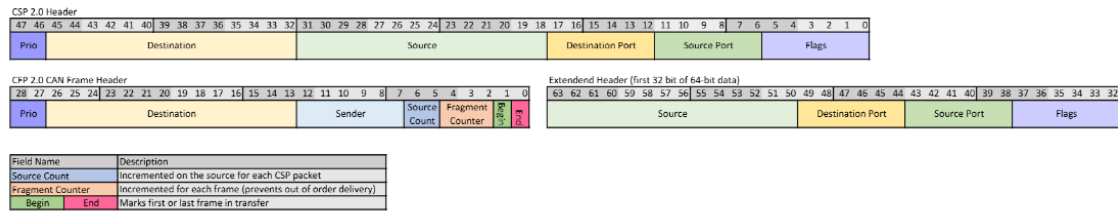


Figure 2.4: The Cubesat Space Protocol header (libcsp authors, 2023a)

A fragmentation protocol is also present to support the CAN frames, which contain at most 8 bytes of data. The I2C interface is supported but it has not been tested with CSP 2.0 (yashi, 2023).

2.4.3 The Space Packet Protocol

The Space Packet Protocol (SPP) is a standard developed by the CCSDS as a replacement for the Packet Telemetry, Telecommand and Advanced Orbiting System (AOS) standards (CCSDS, 2023). The SPP is designed to contain an Application Process IDentifier (APID) within the header to identify the data contents, data source and/or data user (i.e. destination) within a given enterprise. The goal is to efficiently transfer space application data of various types and characteristics between nodes, over one or more subnetworks, and possibly involving one or more ground-to-space, space-to-ground, space-to-space, or on-board communication links (CCSDS, 2020).

The SPP is able to provide the functionality of an Application Layer protocol or can be a “shim” between different layers of a protocol. The collocation of the SPP within the Consultative Committee for Space Data Systems (CCSDS) protocol stack (defined on the basis of the OSI model (CCSDS, 2023)) is illustrated in Figure 2.5, where the corresponding OSI layers are identified by the curly brackets on the left. The SPP has no networking capabilities and relies on the services provided by the application subnetworks.

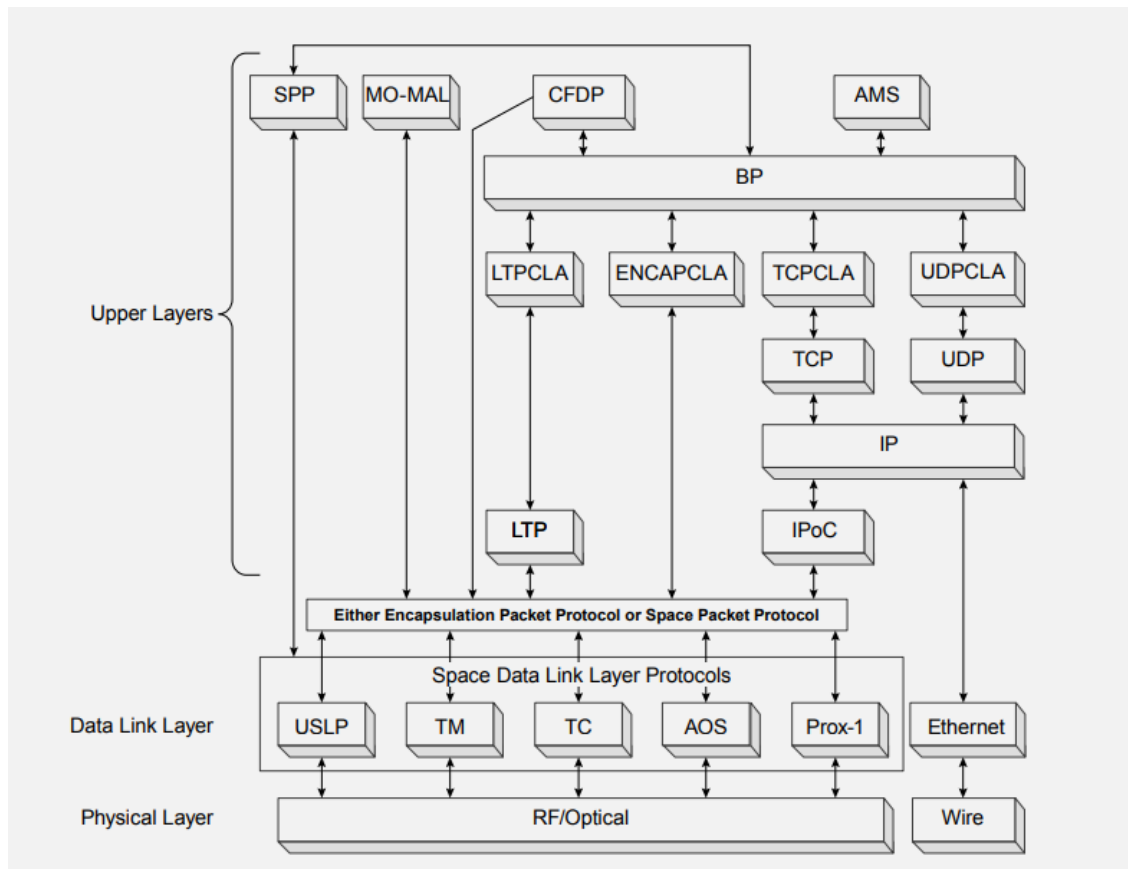


Figure 2.5: The SPP context within the CCSDS Protocol Stack (CCSDS, 2020)

A SPP packet contains a primary header of 6 octets, optionally a secondary header of variable length and the user data field, which together with the secondary header contains at most 65536 octets. The primary header contains four fields: packet version number, packet identification field, packet sequence control field, packet data length. The packet secondary header structure is application dependent and its presence is mandatory only if no user data field is present in the packet. If present, the secondary header shall contain a time code

field only, an ancillary data field only or both (CCSDS, 2020).

An implementation of the SPP is available from the Libre Space Foundation as part of the Open Space Data Link Protocol library (Libre Space Foundation, 2023).

3

Facility design

The review of existing facilities presented in Section 1.3 highlights that a single facility which is flexible enough to host the most common sizes of CubeSats, with physical stimulus of actuators and sensors, to perform HIL simulations does not exist. At the same time, such a verification & validation facility is considered to be a tool to support a “test-as-you-fly” philosophy to mitigate in-flight issues with the ADCS by ESA. The facility can also provide a tool for testing HIL new ADCS techniques and to potentially increase their TRL. The development of such a facility bridges a gap in simulation facilities, both in terms of the facility’s capabilities and its performance. Its development is based on requirements provided by the European Space Agency through a statement of work ([ESA-TECSAA, 2022](#)).

This chapter describes the design of the CubeDynA facility, with focus on the contribution of the PhD candidate. Section 3.1 presents the customer requirements for the facility, Section 3.2 describes the facility architecture, while Section 3.3 gives a description of the facility subsystems, Section 3.4 explores in particular the design of the mas balancing system. Section 3.5 describes the communication module design.

3.1 Facility requirements

The presentation of the design choices made in defining the facility architecture can not prescind from the description of the project requirements. This section, accordingly, describes the requirements for the air bearing facility.

The goal is to use the air bearing facility both for open-loop and closed-loop functional testing. The open loop testing can consist of sensor-to-actuator functional chain polarity testing or coarse performance assessment for attitude sensor & estimator functional chains; the closed loop testing (or test-as-you-fly) can consist of magnetorquer-based or reaction wheel-based ADCS modes (3-axis detumbling, 3-axis slews and coarse pointing, spin-stabilised modes). Additionally, the facility can be used for coarse sensor characterisation/calibration (magnetometer-measured bias, scale factor, orthogonality and noise as a function of spacecraft state / current-telemetry).

To achieve such functionalities, the facility is envisaged to have the following simulation capabilities for sensor stimulus:

1. magnetic stimulus in inertial frame, generated in laboratory frame, linked to the simulated orbit ephemeris;
2. star-tracker replaced measurements in simulated star-tracker frame, linked to the simulated orbit ephemeris and air bearing platform attitude;
3. Sun simulation in an inertial frame, with fixed position in the laboratory frame, with the possibility to simulate different beta angles;
4. gyroscopes stimulus in body frame, via air bearing platform motion.

The formal facility requirements are here presented, with distinction between functional requirements, performance requirements, sensor stimulation requirements and ground station equipment infrastructure requirements. Only the requirements which are relevant for the facility's technical development are presented.

Table 3.1: Facility requirements

Requirement ID	Requirement text (extract)
----------------	----------------------------

General Functional Requirements	
AOC-TB-01	The facility shall include a spherical air bearing capable of 3 degrees of freedom attitude motion.
AOB-TB-02	The facility shall be compatible with operating in an ISO 8 class clean room/tent.
AOC-TB-03	The facility shall allow the satellites specified in AOC-TB-15 to be placed on the air bearing mount platform in any orientation where the CubeSat is resting on one of its six sides, with mechanical contact only on the rails.
AOC-TB-04	The facility shall include automated mass balancing, with any sensing and actuation independent of the item under test (CubeSat), to minimise the efforts to set up a test.
AOC-TB-07	The platform-independent orientation measurements (AOC-TB-22) shall be usable in real-time for driving attitude-dependent stimulus.
AOC-TB-11	The facility shall include mechanical Ground Support Equipment to allow lifting of 6U and 12U satellites from a transport/storage container onto the air bearing table and supporting their weight during installation on the table.
AOC-TB-13	The facility shall fit into a 2.7 m × 2.7 m × 1.95 m (height) lab space.
AOC-TB-14	The facility shall be re-locatable via wheels at the base that can also be locked via brakes.
Performance Requirements	

AOC-TB-15	The facility shall support the following CubeSat sizes and masses: • 1U (10 cm × 10 cm × 11.35 cm, <2 kg) • 3U (10 cm × 10 cm × 34.05 cm, <6 kg) • 6U (20 cm × 10 cm × 34.05 cm, <12 kg) • 12U (20 cm × 20 cm × 34.05 cm, <24 kg) [as per CubeSat Design Spec., CP-CDS-R14.1 (SLO, 2025)] with additional margins of 2 kg and 15 cm per dimension for table-mounted Ground Station Equipment (GSE).
AOC-TB-16	The facility shall support the following c.g. offsets from the geometric CubeSat centre point: • 1U: <2 cm per-axis • 3U – 12U: <7 cm per-axis
AOC-TB-17	The facility shall include a stand-alone compressor for powering the air bearing for 3 hours (2 low-Earth orbits) of continuous testing.
AOC-TB-19	The air bearing disturbance torques (including friction and mass imbalance) shall have RMS values lower than: • 2×10^{-5} N m for 1-6U cubesats • 5×10^{-5} N m for 12U cubesats
AOC-TB-20	The air bearing table shall support the following motion range: • Roll/Pitch: $\pm 30^\circ$ • Yaw: unlimited • Roll/Pitch rates up to 5° s^{-1} per axis • Yaw rates up to 30° s^{-1}

AOC-TB-21	The facility shall have the ability to start tests with initial attitudes different from lab-horizontal (0° pitch & roll) and initial rates up to 30° s^{-1} .
AOC-TB-22	The facility shall include independent measurements of platform orientation with fixed bias (or calibration residual) $<0.1^\circ$ per-axis and attitude-dependent/time-varying error $<0.01^\circ$ RMS per-axis at a sample rate of up to 10 Hz, with both latency and timestamp knowledge error (vs GSE reference) less than 0.1 s.
Sensor Stimulation Requirements	
AOC-TB-23	The facility shall be capable of compensating the local magnetic field and emulating the Earth's on-orbit magnetic field with arbitrary vector direction and magnitude up to $600 \mu\text{T}$ with an error $<1\%$ per axis across the Cube-Sat working area (diameter 34 cm).
AOC-TB-24	The facility shall include a Sun lamp with the following properties: • Angular divergence $\approx 0.53^\circ$ • Spatial non-uniformity $<10\%$ • Illumination working diameter $\geq 34 \text{ cm}$ • Spectral content and intensity aimed at stimulating photodiode or photocell sensors at near 1 solar constant at 1 AU • A high (~ 1 solar constant) and low intensity option shall be present.
AOC-TB-25	The Sun lamp (or Sun beam relay mirror) shall be manually movable along a quarter-arc ranging from the overhead position to the horizontal position.
AOC-TB-26	It shall be possible to emulate eclipse.

AOC-TB-27	It shall be possible to optionally emulate Earth's albedo (mean albedo level) only for short duration tests (where the albedo source location with respect to the spacecraft can be assumed static).
GSE Infrastructure Requirements	
AOC-TB-28	The facility shall include real-time (hard or soft) software that is capable of: • Performing the mass balancing • Air bearing control (as needed) • Setting simulated date and frame offset between inertial J2000 and lab frame (fixed during each test) • Propagating orbit kinematics • Simulating a Sun ephemeris model • Estimating the platform ground-truth attitude (can include noise filtering if necessary, with a cut-off higher than 10 Hz) • Magnetic stimulus • Star tracker stimulus • Logging all relevant data from the test • Real-time visualisation of orbit and attitude
AOC-TB-32	The Sun lamp (or Sun beam relay mirror) shall be manually movable along a quarter-arc ranging from the overhead position to the horizontal position.
AOC-TB-33	It shall be possible to emulate eclipse.
AOC-TB-34	It shall be possible to optionally emulate Earth's albedo (mean albedo level) only for short duration tests (where the albedo source location with respect to the spacecraft can be assumed static).

3.2 Facility architecture

The CubeDynA facility is composed of many subsystems which interact to generate a simulation environment for CubeSat ADCS. A distinction is made between the Air Bearing Table (ABT) subsystem and the Physical Stimulus (PS) subsystem. The former provides the 3-DOF low-torque rotational motion, electrical power and communication interface to the CubeSat. The second provides physical stimulus to the ADCS sensor. The distinction is not rigorous (for instance, the air bearing provides physical stimulus to inertial sensors), but it useful for the sake of discussion. Figure 3.1 shows the product tree of the facility. The ABT subsystem includes the following components:

- an air bearing module, a compressor and an air conditioning unit;
- two air bearing tables for hosting of:
 - ABT platform electronics for mass balancing;
 - ABT platform electronics for the communication interface;
 - regulated power for the DUT and the platform electronics;
 - mechanical interfaces for hosting CubeSats;
- Mechanical Ground Station Equipment (MGSE) for handling of heavy CubeSats;
- custom pedestal with adjustable height;
- safety net and cushioning for safety hazard minimisation.

The PS subsystem includes the following components:

- a Helmholtz cage for magnetic field generation;
- a custom Sun simulator for simulation of Sun radiation;
- an albedo lamp for simulation of Earth's albedo;
- a ground-truth system for independent attitude estimation and star tracker simulated measurements.

Figure 3.2 shows instead an architectural diagram of the facility. The user interacts with the laboratory computer, which runs the facility software. The user-machine interaction is facilitated through a GUI. The facility software functionalities include:

- synchronization of the facility PS components;
- definition and run of simulation scenarios;
- logging of the simulation data;
- mass balancing;
- user interaction;

The laboratory computer uses serial and Ethernet interfaces to communicate with the Helmholtz cage and the ground truth system. The facility includes a Local Area Network (LAN) for interfacing the laboratory computer with the balancing electronics and the DUT through the communication module. Indeed, to provide a low torque environment no cable connection should be present between the rotating platform and the rest of the laboratory. Additionally, a customer ground station can be interfaced with the DUT through the communication module.

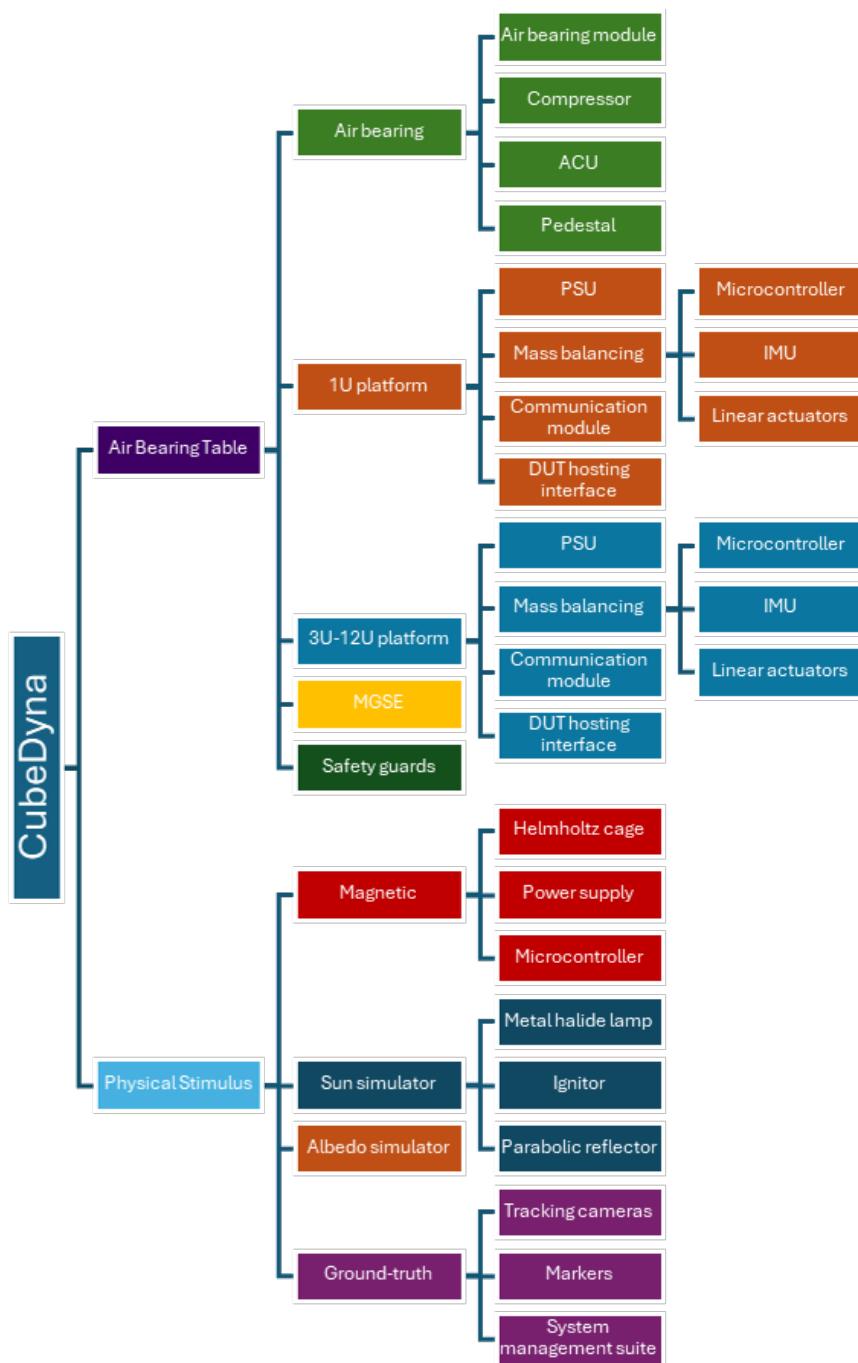


Figure 3.1: CubeDynA product tree

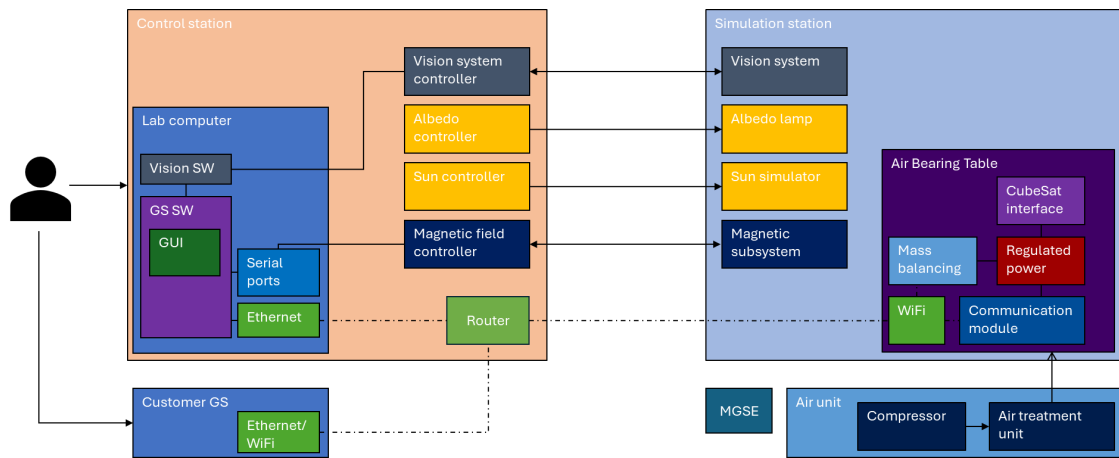


Figure 3.2: CubeDynA architectural diagram

3.3 Facility subsystems

A core contribution of this doctoral work concerns the development of the automatic mass-balancing system for the CubeDynA facility. A secondary contribution addresses the communication interface between the DUT and the Ground Station (GS). These two subsystems are described in detail in Section 3.4 and in Section 3.5, respectively.

To place these developments in context, the present section provides a high-level description of the remaining subsystems of the CubeDynA facility. These were largely developed prior to, or independently from, the doctoral work, and are therefore summarised here only to give a complete overview of the testbed architecture.

3.3.1 Magnetic simulator

Satellites in LEO make use of the magnetic field for attitude determination and control. The CubeDynA facility magnetic subsystem can generate the same magnetic field a spacecraft would encounter in a LEO. This allows testing of magnetic sensors and actuators HIL. The magnetic field can be optionally scaled in intensity and frequency to compensate for the effect of disturbance torques. The Helmholtz cage can also be used to generate an arbitrary magnetic field for DUT magnetometer calibration.

At the core of the magnetic stimulus subsystem, there is a tri-axial Helmholtz cage, the Ferronato BH-1300-3-C from Serviciencia, Spain. It features three orthogonal pairs of coils ($D \approx 1300mm$) that can generate an arbitrary magnetic field in the range ± 10 Gauss. This last value is far larger than the magnitude expected in orbit (< 0.5 Gauss): during tests, since the disturbance torque magnitude affecting the facility is larger than the orbital one, the Helmholtz cage generated field may be scaled up in intensity to enhance the magnetorquers control authority. The power supply limits the actual maximum magnetic field. In this case a triple-channel programmable power supply, the Rohde & Schwarz HMP4030 is used, the maximum power per channel of 160 W limits the magnetic field at about $6 G = 6 \times 10^{-4} T$ per axis. An Arduino UNO and switch relays are used to be able to command current in different directions inside the coils. The laboratory magnetic field is estimated using the XSens MTi 3.0 DK

IMU magnetometer and compensated. The Arduino UNO is controlled by the computer running the main facility software. Figure 3.3 shows the connections of the magnetic subsystem.

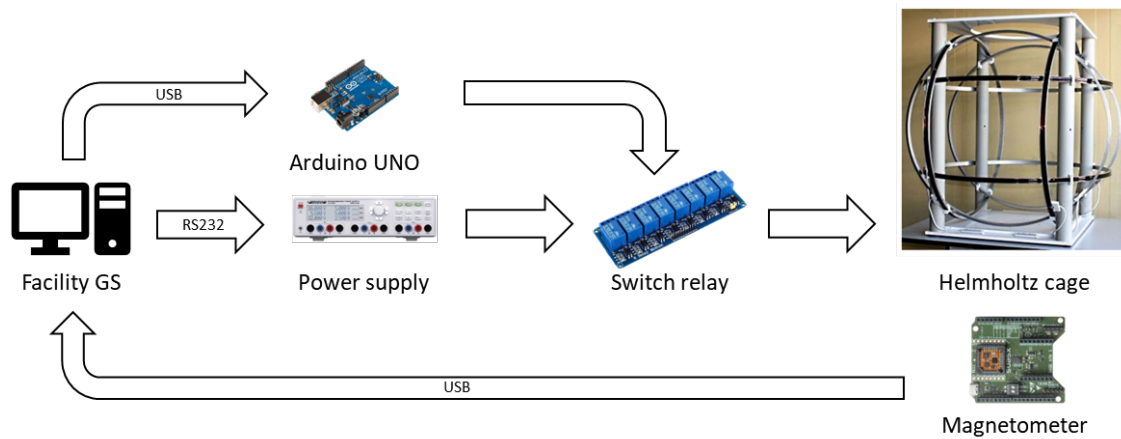


Figure 3.3: Magnetic subsystem connections

3.3.2 Sun simulator

A Sun simulator is required for the facility to act as a stimulus to Sun sensors carried onboard the device under test. A Sun simulator is classified according to three criteria, namely: (a) spectral matching, (b) spatial uniformity, and (c) temporal stability. For testing a Sun sensor, other parameters are of importance, such as the collimation of the light beam over a wide area, which shall be maintained close to 0.53° i.e., the apparent angular diameter of the Sun at 1 AU, and the power flux level (1367 W m^{-2} at 1 AU) at the nominal target distance. Differently from LED lamps, Metal Halide (HMI) lamps can provide good spectral matching out of the visible spectrum. A custom solution employing a metal halide bulb and a parabolic reflector is used. The bulb is placed at the focus of the parabolic reflector, the light is in this way collimated. Beam intensity uniformity is achieved using neutral density filter sheets. The Metal Halide bulb and parabolic reflector are housed within a protective enclosure equipped with fans for heat management. A dimmable power supply, coupled with a high-voltage, ensures the bulb ignition and regulation. The Sun simulator is re-orientable in a 90° angle range to simulate different orbit β angles. Figure 3.4 shows the Sun simulator.

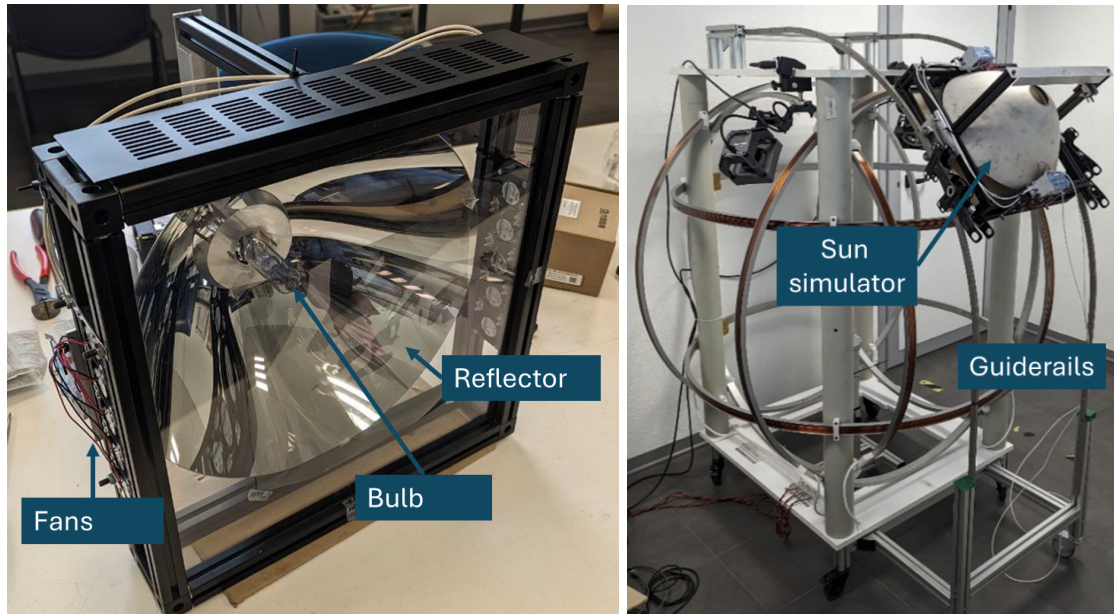


Figure 3.4: The CubeDynA Sun simulator

3.3.3 Albedo lamp

For satellites in LEO orbit, albedo radiation is perceived as an extended, diffuse source of light. LED lamps are well suited for creating a diffuse light. A COTS LED lamp is used for the CubeDynA facility. The sizing for the albedo lamp is done taking into account an average Earth's Albedo coefficient of 0.4, Earth-Sun sensor view factor of 0.5 and the satellite at sub-solar point. Dimming of light is achieved with a series of film neutral light attenuators. Figure 3.5 shows the CubeDynA albedo lamp.



Figure 3.5: CubeDynA albedo lamp (without neutral light attenuators)

Unlike the Sun direction, which can be considered inertially fixed, the direction of the Earth is not inertially fixed for a spacecraft travelling in LEO. Being the Albedo lamp fixed in the laboratory frame, the realism of the albedo stimulus is limited to few minutes. The Albedo lamp can be mounted on different pillars of the Helmholtz cage structure.

3.3.4 Ground-truth system and star tracker emulator

The ground-truth system provides an independent measurement system of the device-under-test attitude. This is based on the COTS Optitrack PrimeX 22 dual camera system, composed of:

- marker assembly, a tri-dimensional geometry composed with high reflective markers;
- camera assembly, designed to capture a stereo scene in the infrared band;
- system management suite, intended to set the working parameters, calibrate the system, estimate the attitude and dispatch it to the main application.

The measurements coming from the ground-truth system and provided in the camera frame, are transformed into the laboratory frame and in the simulated frame. These steps require a calibration procedure of the ground-truth system. Figure 3.6 shows the ground-truth system cameras and a few markers placed on the air bearing table.

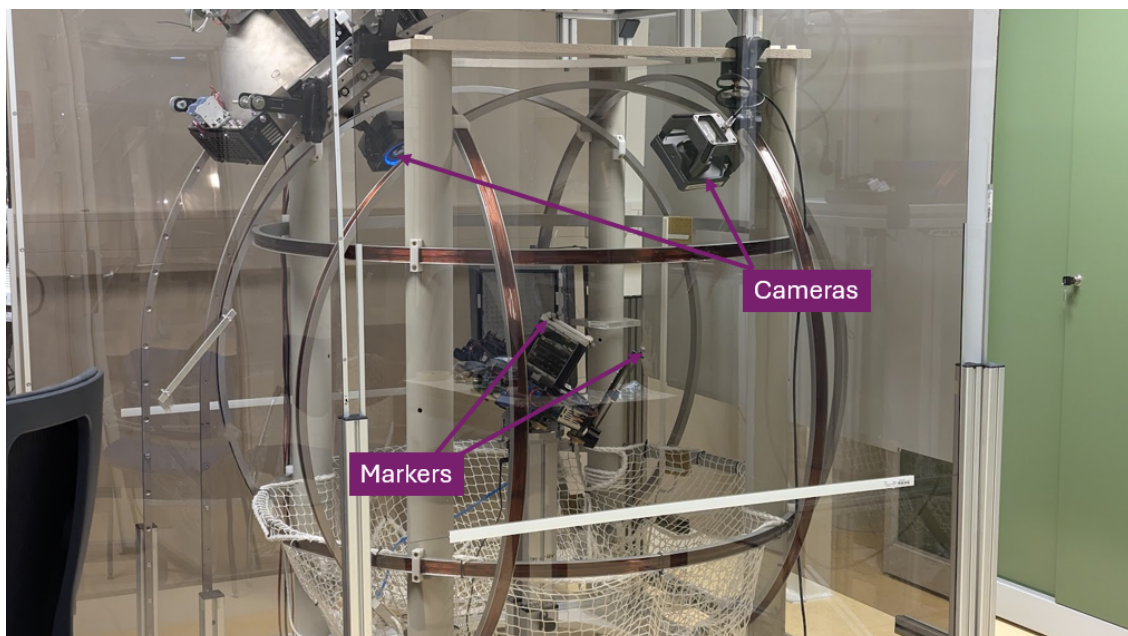


Figure 3.6: CubeDynA ground-truth system

The attitude estimated from the ground-truth system is used for the star tracker emulator. This provides the payload interface with a quaternion (possibly corrupted by a user-defined error) at a specific time-rate. The main facility software checks if the Moon, Sun and Earth are in visibility of the emulated star-tracker field of view and applies a flag to the packet containing the quaternion accordingly.

3.3.5 Air bearing

The 3-DOF rotational motion is provided to the DUT through a hemispherical air bearing. The CubeDynA facility employs the A-653.045 module of Physik Instrumente, compatible with clean tents. The air bearing allows a motion range of $\pm 45^\circ$ in pitch/roll and unlimited in yaw with very low friction and can support a load up to 1600 N. The actual rotational range is limited to $\pm 30^\circ$ in pitch/roll by the air bearing table. The air bearing module consists of two parts: a convex hemisphere, which moves with the air bearing table, and a fixed stage, with matching concave surface, which provides the pressurised air flow. The fixed stage is mounted on a custom pedestal and is powered by a compressor. The air flowing through the air bearing should be clean and free from oil. For this scope, the Air Conditioning Unit (ACU) A-801.200 from PI is used. The ACU receives air from the Werther Sil-Air 50/24 low-noise air compressor. In order to minimise the turbine torque, the pressure is adjusted to the minimum level which makes the air bearing to float, and this value will value with payload mass. Figure 3.7 shows a picture of the air bearing module together while Figure 3.8 shows a picture of the compressor and ACU.

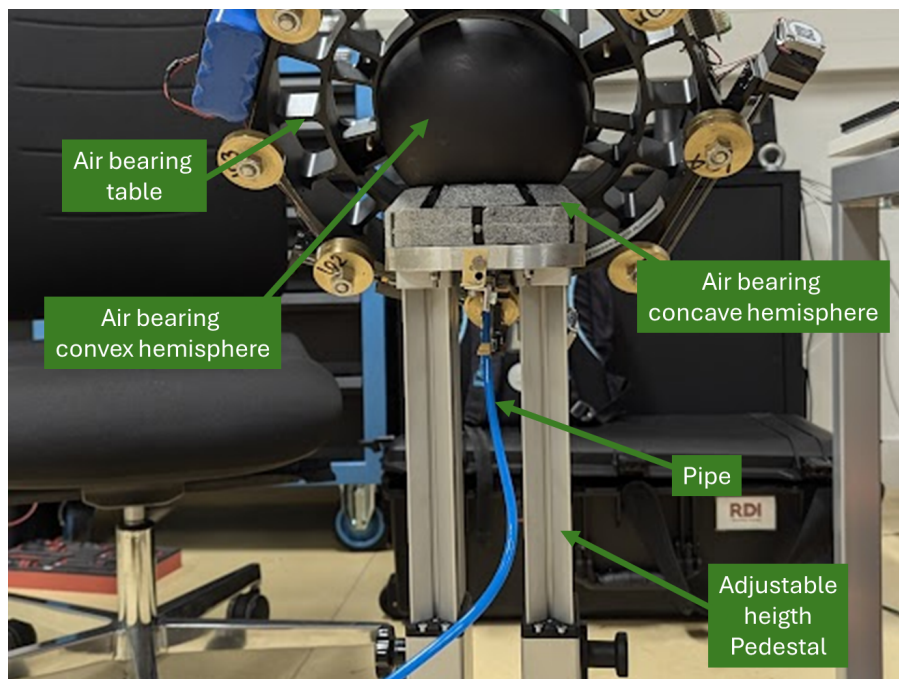


Figure 3.7: Air bearing module on the pedestal and air bearing table

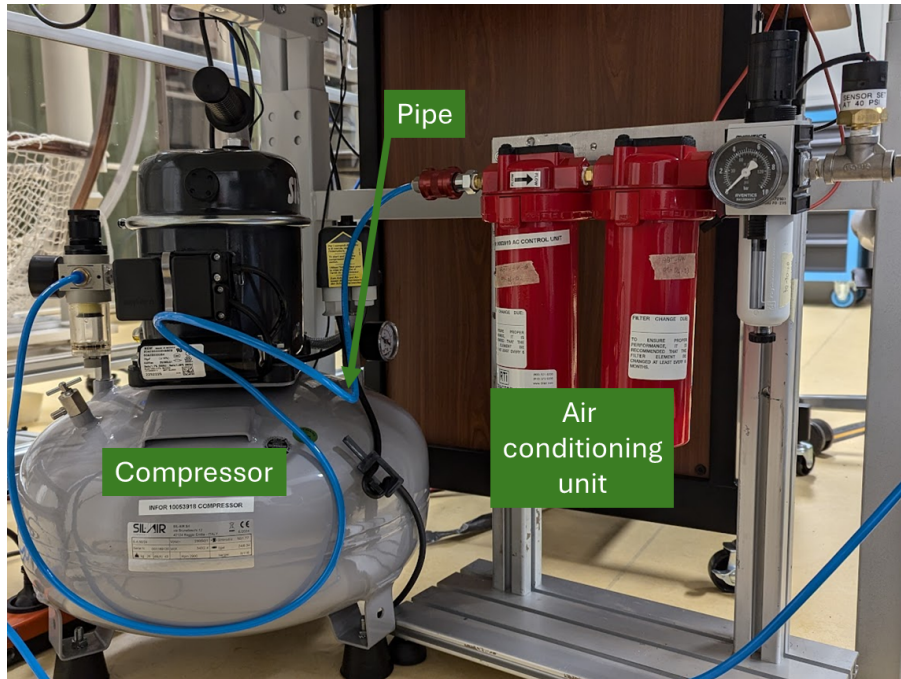


Figure 3.8: Compressor and air conditioning unit

The attitude estimated from the ground-truth system is used for the star tracker emulator. This provides the payload interface with a quaternion (possibly corrupted by a user-defined error) at a specific time-rate. The main facility software checks if the Moon, Sun and Earth are in visibility of the emulated star-tracker field of view and applies a flag to the packet containing the quaternion accordingly.

3.3.6 Air bearing table and CubeSat mounting

The air bearing table constitutes the mechanical interface between the air bearing and the DUT. Furthermore, it hosts the mass balancing components, the communication module and the Power Supply Unit (PSU). The ABT is of the tabletop type and it is directly mounted on the air bearing convex hemisphere. The mass balancing system and the air bearing itself modify the inertia of the DUT. To mitigate this issue, two ABT platforms were developed: one for hosting 1U CubeSats, whose mass does not exceed 2 kg, and one for hosting CubeSats of the 3U, 6U and 12U factors, whose mass can reach 24 kg.

The ABT are two partially hollow aluminium octagonal prisms, designed to maximise the stiffness-to-mass ratio. This has the double scope of reducing the disturbance torque due to sagging and to minimise the increase in inertia. The octagonal shape allows the placement of the components on the prism's lateral surfaces. The hollow structure allows placing the air bearing inside the platform while shifting the CM downward. Counterweights are placed beneath the platform by means of eight threaded rods. Indeed, by placing the DUT on the ABT, the CM is shifted above the CR, which coincides with the GC of the air bearing. Counterweights should then be placed beneath the ABT. For very light CubeSats, it could be necessary to shift the CM upwards; in this case, the distance between CR and ABT is increased using spacers.

Dedicated mounting mechanisms have been designed for each size of supported CubeSat. It is ensured that the contact with the CubeSat happens only through the CubeSat rails, analogously to what happens with dedicated CubeSat deployers. A custom lifter was manufactured as Mechanical Ground Station Equipment (MGSE) to hold the weight of heavier CubeSats. Figure 3.9 shows 1U, 3U, 6U and 12U CubeSats mock-up mounted on the ABT. These mock-ups are designed to reproduce the geometry and CM envelope as in the standard (SLO, 2025)¹. Figure 3.10 shows examples of the MGE use.

¹All the mock-ups have been designed at the u3S Lab, apart from the 6U that was kindly provided by NPC-SpaceMind Company – Faenza, whose support is highly appreciated

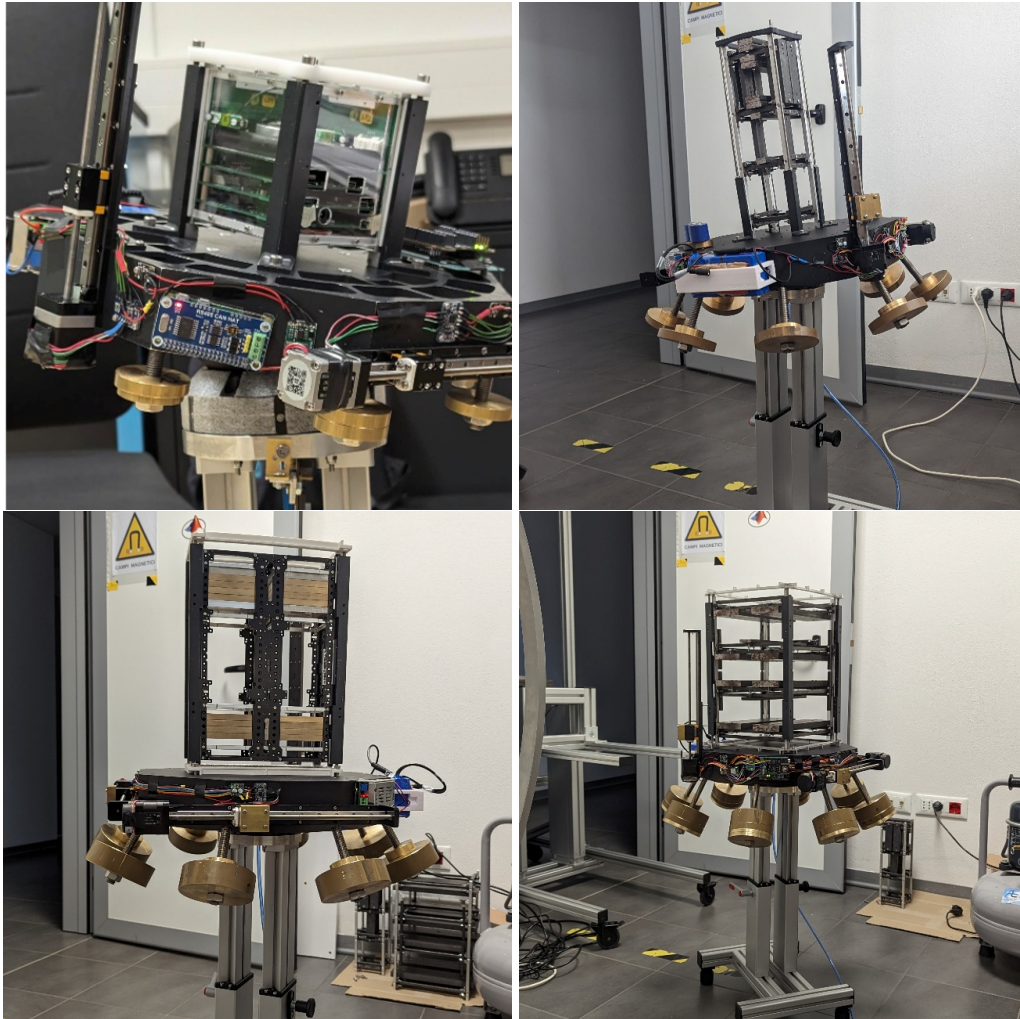


Figure 3.9: The air bearing table platform with on top different CubeSats mock-ups and relative counterweights. Top-left: 1U CubeSat; top-right: 3U CubeSat; bottom-left: 6U CubeSat, bottom-right: 12U CubeSat

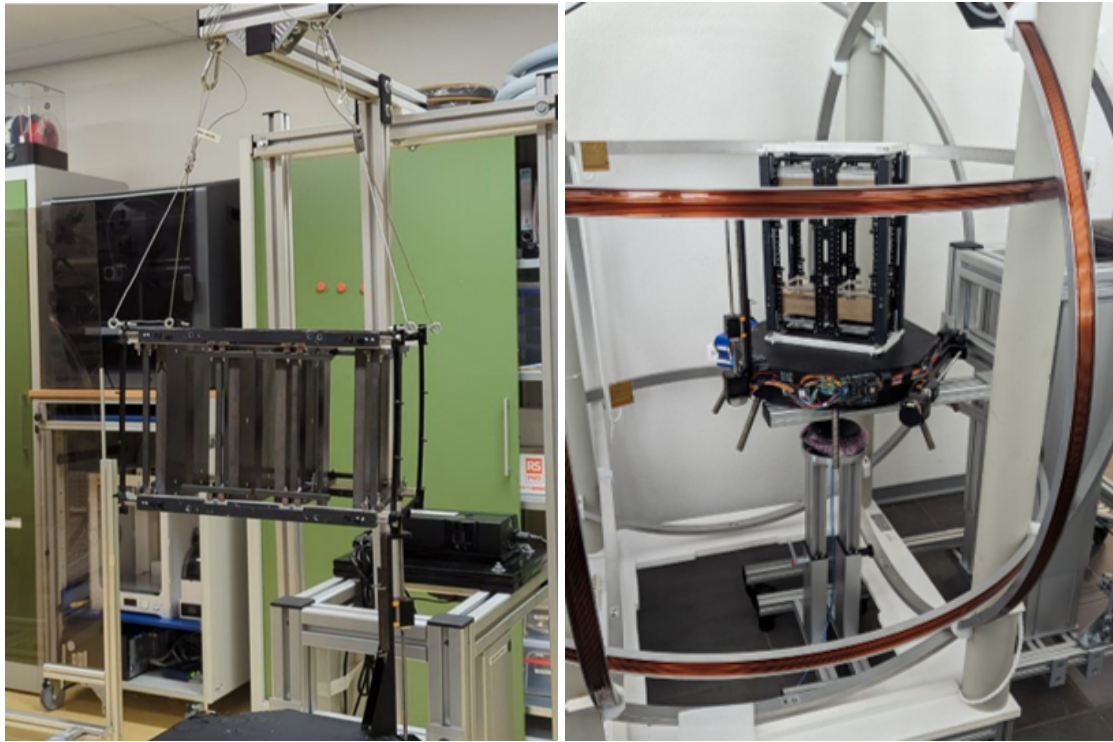


Figure 3.10: The CubeDynA MGSE, holding 12U mock-up: left, holding ABT and 6U mock-up: right

3.4 Mass balancing

The gravity torque is the biggest disturbance torque acting on air bearing facilities (Bahu, 2021). This is caused by the misalignment between the CM and CR of the rotating platform. A mass balancing system shifts the position of the CM to bring it close to the CR, hence reducing the gravity torque. As presented in Section 2.3, the mass balancing system design shall address two opposing key requirements: it should have enough control authority to compensate for large and diverse CM to CR offsets; at the same time, it should have good enough resolution to achieve low gravity torque. The first requirement is satisfied by using a large value of balancing mass and lever arm, while the second is satisfied by using a small value of balancing mass and fine position control. To address both requirements, the mass balancing system is divided into two parts: coarse mass balancing and fine mass balancing. The coarse mass balancing consists in the use of balancing masses or counterweights to compensate for the macroscopic distance between CM and CR. Once this distance is within the range of the fine balancing system, fine balancing is performed. During fine balancing, the CM is brought at sub-micron distance from the CR.

Following the approach developed for the heritage ADCS facility of the University of Bologna (Modenini et al., 2020), (Bahu and Modenini, 2020), fine balancing is divided into two steps: in-plane balancing, where the component of the CM to CR offset perpendicular to the gravity vector is compensated, and a system identification part, where the component of the CM to CR offset parallel to the gravity is estimated together with the platform inertia. The residual gravity torque is estimated and the two steps are repeated until no further improvement is observed.

The coarse balancing procedure and sizing are described in Section 3.4.1. The fine mass balancing system is described in Section 3.4.2; Section 3.4.2.6 describes an alternative balancing method employing both mechanical and magnetic actuation.

3.4.1 Coarse balancing

Coarse balancing is performed by distributing mass balancing disks on the 8 ABT platform pins, shown in Figure 3.11. To limit encumbrance, a material with

high density should be used to manufacture the mass balancing disks. Among machinable materials, lead would be the first option, given its high density (11.3 g cm^{-3}). Lead, however, is toxic when reduced to powder. For this reason, brass has been chosen (density 8.5 g cm^{-3}).

Given the range of CubeSat mass size which shall be hosted on the platform and their relative CM position variation, a single set of counterweights does not fit all the configurations. Instead, a set of mass balancing disks which covers an envelope of possible test cases should be sized. Counterweights sizing is performed by imposing that the sum of first moments about the CR of the rotating platform and balancing weights is equal to zero:

$$\sum_{i=1}^N m_i \mathbf{r}_i = \mathbf{0} \quad (3.1)$$

Equation (3.1) is vectorial and should be solved for all three coordinate components. The imbalance in the z axis is, however, the driving factor because of the greater distance between CR and satellite CM in this direction. Unbalance in the x and y axes is instead obtained by distributing the counterweights on the 8 pins. The mass of the counterweights can then be obtained as:

$$m_{disks} = \frac{\sum_{i=1}^{N-1} m_i r_{i,z}}{r_{disks,z}} \quad (3.2)$$

where the sum at the numerator represents the contribution to the balance of all the components except the mass balancing disks, and the denominator is the position of the balancing disks on the threaded pins. This is, in principle, another free variable. To obtain a value for the mass balancing mass, its value can be taken as an initial guess. The coarse balancing mass is computed and, if any infeasibility is found, the position can be changed for a second guess. The mass balancing weights indeed can only assume discrete values. The sum will be in general different from zero. The goal is to obtain a residual CM to CR offset small enough to be compensated by the fine balancing system. Given the stepper motor carrier motion (half) range and their mass, the fine balancing system can balance a residual moment of 0.014 kg m on the 1U platform and 0.106 kg m on the 3U-12U platform.

To ease the computation of the coarse balancing weights, an Excel spreadsheet has been created. Portions of the spreadsheet are shown in Figure 3.12 and Fig-

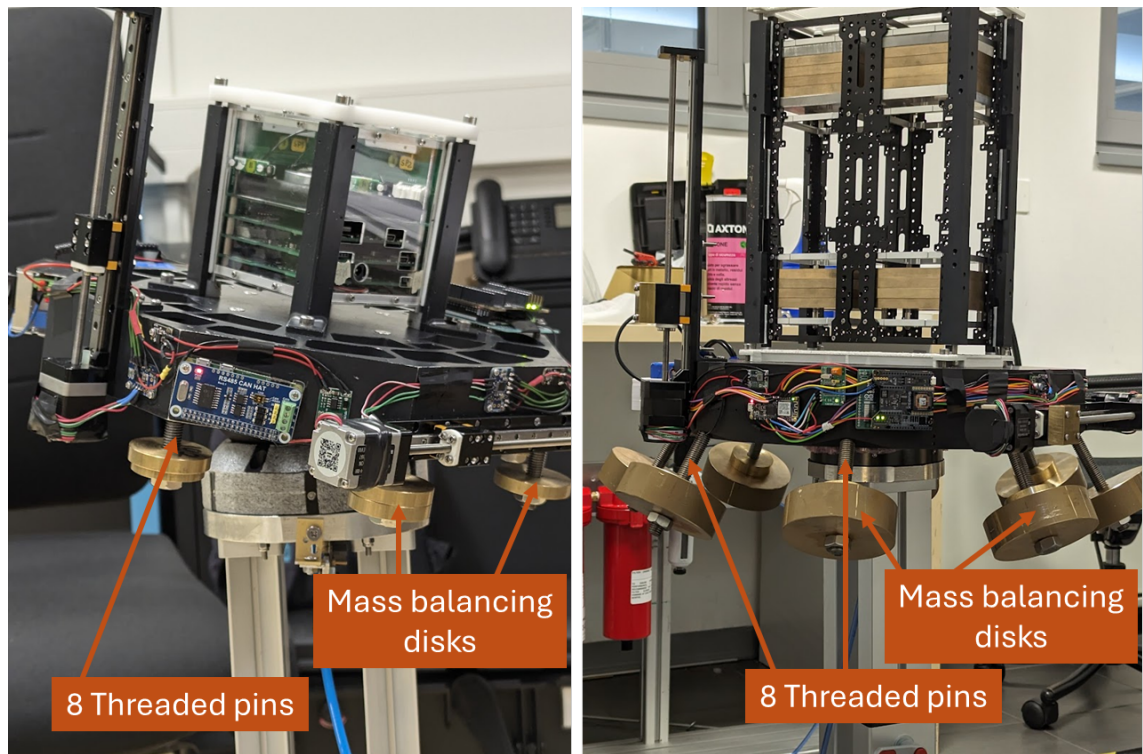


Figure 3.11: CubeDynA coarse mass balancing: threaded pins and mass balancing disk

Figure 3.13. The user inputs the mass of the DUT, its CM position relative to its GC, the mass of the Ground Station Equipment (GSE) and its CM relative to the ABT platform plane, the distance at which the mass balancing disks are mounted and the spreadsheet computes the coarse mass balancing mass. The different types of mounting are taken into account by selecting the DUT orientation from a drop-down menu, linked to the datasheet. For very light DUT, it can be that the CM is already above the CR without the use of counterweights. In that case, the resulting balancing mass will be negative and the cell containing its value will be coloured in red. Since the mass can be added only below the CR through the pins and not above it, spacers can be mounted in between the air bearing module and the ABT to shift up the CM position. These are shown in Figure 3.14. In this case, another drop-down menu within the spreadsheet allows for the selection of which spacer to use. Three spacers have been manufactured, with thicknesses of 5.3, 5.6 and 10.3 mm, providing seven possible combinations. For particularly heavy CubeSats instead, the distance from the ABT at which the counterweights are mounted can be extended by using a set of balancing disks which

are threaded on two opposite sides in combination with another set of threaded pins. These are shown in Figure 3.15.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]	Spacer type	Spacer thickness [mm]
CubeSat 1U	2000	76.75	DUT base	180100	No spacer	0.00
GSE 1	2000	20.00	Platform plane	46600		
GSE 2	0	0.00	Platform plane	0		
1U Platform	3865	-22.06	Platform plane	-72517		
Air bearing	1444	-33.11	Air bearing top	-47811		
Counterweights total	1595	-20	Platform bottom	-106372		
Counterweights/8	199					
Total mass	10904		SUM	0		

Figure 3.12: Screenshot of coarse mass balancing spreadsheet, 1U platform

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]	Spacer type	Spacer thickness [mm]
CubeSat 6U	12000	158.15	DUT base	2138400	No spacer	0.00
GSE 1	2000	20.00	Platform plane	50000		
GSE 2	0	0.00	Platform plane	0		
3U-12U Platform	11353	-15.12	Platform plane	-114892		
Air bearing	1444	-33.11	Air bearing top	-47811		
Counterweights total	16055	-75	Platform bottom*	-2025697		
Counterweights/8	2007					
Total mass	42852					

* NB: the height at which the counterweights are mounted is measured along the pin axis

Figure 3.13: Screenshot of coarse mass balancing spreadsheet, 3U-12U platform

The sizing of the counterweights has been performed taking into account a few extreme scenarios, in which the CubeSats CM position is at the limit values specified by the standard. The limit cases in which the CM of the CubeSat is at its lowest and highest position have been considered, as well as the ones in which the mass of the CubeSat is at its lowest or highest expected value. When the mass and CM positions are at their lowest value, it is also assumed that no GSE is needed, further lowering the CM position. Considering the CubeSat axes as defined in the design specification (see Figure 1.3), the vertical (V) orientation is defined as the one which aligns the CubeSat z axis with the platform z axis. Conversely, the horizontal (H) orientation is defined as the one which aligns the



Figure 3.14: Spacers used to shift up the CM position. The picture on the left shows the spacers on the air bearing module, the picture on the right shows the spacers on the 1U platform

x axis (horizontal 1 or simply horizontal) or the y axis (horizontal 2) with the platform z axis. The sizing scenarios are presented in Table 3.2 together with the resulting mass balancing values. The CM offset from the geometric centre is defined positive when above the CR, negative when below. In the sizing process, the mass of the ABT platform also includes the masses of the components mounted on it (battery pack, stepper motors, etc.). The results of the mass balancing spreadsheet for each case are reported in Chapter A.

Table 3.2: Coarse mass balancing sizing scenarios (H = Horizontal, V = Vertical, SI = SpaceInventor)

Case N.	DUT	Mount.	Mass [kg]	GSE	CM to GC [mm]	Bal. mass [kg]	Total mass [kg]
1	1U	V	2.0	Yes, 2kg	+20	1.595	10.904

2	1U	V	1.0	No	−20	0.792	7.101
3	1U	H	2.0	Yes, 2kg	+20	1.59	10.899
4	1U	H	1.0	No	−20	0.808	7.117
5	SI	/	2.0	Yes, 2kg	0	0.449	9.758
6	3U	V	6.0	Yes, 2kg	+70	10.51	31.307
7	3U	V	3.0	No	−70	1.608	17.405
8	3U	H	6.0	Yes, 2kg	+20	4.002	24.799
9	3U	H	3.0	No	−20	0.798	16.595
10	6U	V	12.0	Yes, 2kg	+70	24.062	50.859
11	6U	V	6.0	No	−70	4.8	23.597
12	6U	H	1 12.0	Yes, 2kg	+45	16.055	42.852
13	6U	H	2 6.0	No	−20	1.591	20.388
14	12U	V	24.0	Yes, 2kg	+70	31.189	69.986
15	12U	V	12.0	No	−70	10.413	35.21
16	12U	H	24.0	Yes, 2kg	+45	21.649	60.446
17	12U	H	12.0	No	−45	6.425	31.222

Overall, the balancing mass needed depends on both the DUT mass and its CM position, as evident from Table 3.2. The set of counterweights that has been manufactured is reported in Table 3.3.

The value of the coarse balancing mass shall make it possible to perform the fine balancing step. In particular, the platform should have its CM position below the CR when the carrier of the linear actuator aligned with the z axis is at its bottom-most position and above the CR when the carrier is at the highest position. This ensures that there exists a position of the carrier which makes the CM to coincide with the CR, this is called the neutral point. The right amount of counterweights

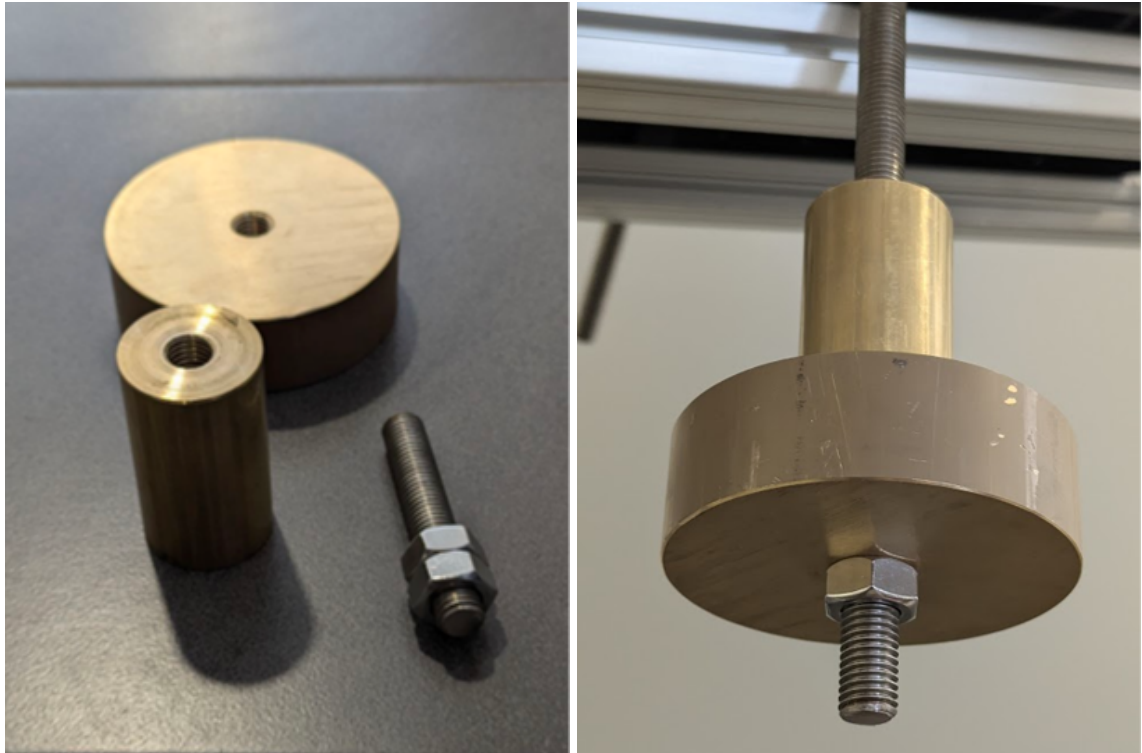


Figure 3.15: Mass balancing disks and threaded pins used to extend the distance between ABT and counterweights

is then the one that will make the platform oscillate as a *stable pendulum* when the stepper motor carrier is at about its lowest position, as an *unstable pendulum* when the stepper motor carrier is at about its highest position. The platform oscillates as a stable pendulum if, when rotated from its equilibrium position, it will come back to it with oscillations around this point; the platform acts as an unstable or inverted pendulum if it will tend to tilt on one side. This procedure is summarised by the diagram in Figure 3.16.

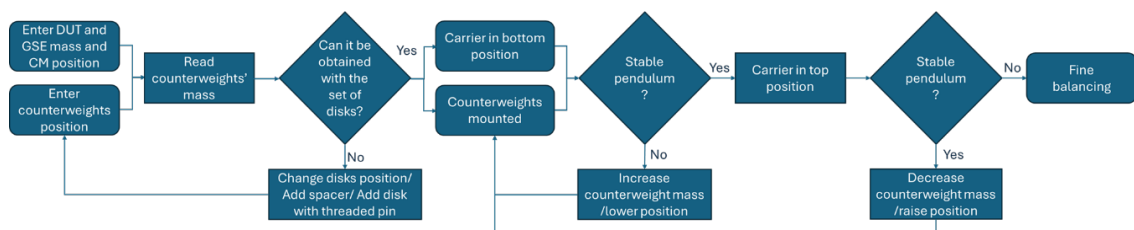


Figure 3.16: Coarse balancing decision diagram

Table 3.3: Set of counterweights

Platform	Quantity	Value [g]	Diameter [mm]	Height [mm]
1U	8	205	45	16
1U	8	102	45	8
1U	8	51	40	5
3U-12U	8	2084	100	32
3U-12U	9	990	100	15
3U-12U	9	520	100	8
3U-12U	8	107	50	7
3U-12U	8	199	50	13
3U-12U	8	665	40	70

3.4.2 Fine mass balancing

The fine mass balancing is divided into two steps: during in-plane balancing, an automatic balancing system shifts three orthogonal masses to bring the CM on the same vertical line of the CR. This operation compensates for the CM to CR offset component perpendicular to the gravity vector. The automatic balancing system is driven by a microcontroller which commands three linear actuators based on attitude and attitude rate measurements from an IMU. The component of the CM to CR offset parallel to the gravity vector, however, cannot be directly sensed by the IMU. This component and the inertia matrix of the system are jointly estimated by sampling and filtering of free-oscillation data of the platform.

3.4.2.1 System architecture

The fine mass balancing system allows controlling the position of the CM of the rotating platform with great precision. A high-level architectural diagram of the fine mass balancing system is shown in Figure 3.17: the ABT hosts an Arduino-based microcontroller which communicates with the IMU for in-plane mass balancing and system identification, command the stepper motors through motor drivers, and send telemetry data to the lab computer through WiFi UDP. The mass balancing GUI running on the ground station software is used to monitor in real time the balancing process, update parameters used for the balancing process (control law gains, control period, etc.) and to perform the system identification steps.

The Adafruit Feather M0 WiFi - ATSAMD21 + ATWINC1500 is used as microcontroller and runs an Arduino code. The motor drivers consist of DRV8833 integrated into a breakout board from Adafruit. 12 I/O digital pins of the board can be used to control the stepper motors via the stepper motor driver. The I2C pins are used to receive data from the on-board IMU.

The on-board IMU consists of the XSens Mti 3.0 DK, integrating the MTi-3 (AHRS) module. The performance in terms of attitude and attitude rate knowledge provided by the manufacturer for the single modules is reported in Table 3.4 and Table 3.5.

Linear actuators are used to shift three masses in three orthogonal directions. Stepper motors allow controlling the shaft rotation with great precision. An-

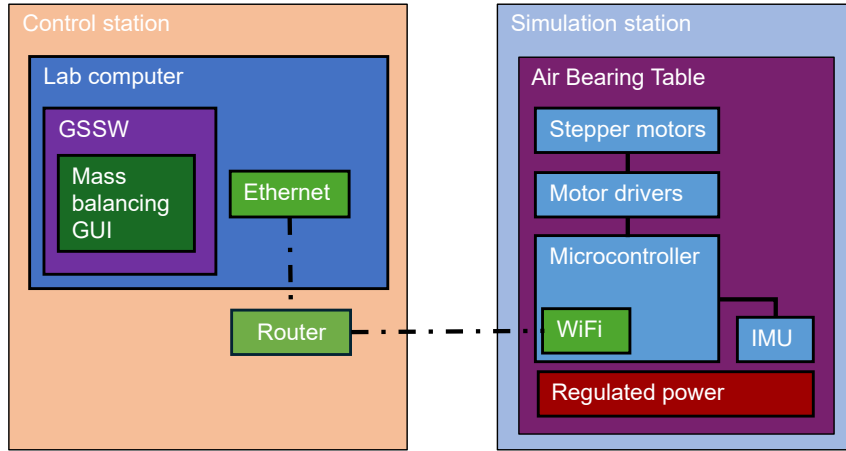


Figure 3.17: High level architectural diagram of fine mass balancing system

Table 3.4: Xsens IMU attitude estimation performance

Parameter		MTi-3 AHRS
Roll/Pitch	Static	0.5°
	Dynamic	0.8°
Yaw	Dynamic	2°

gular steps are transformed into linear steps in linear actuators using a lead-screw. For the CubeDynA facility, the Dings' DLM series of linear actuators is used (DINGS', 2022): 28 DLM150 (NEMA 11) for the 1U platform, 42 DLM250 (NEMA 17) for the 3U-12U platform. Some of the linear actuator technical specifications are reported in Table 3.6.

Power is provided on the 1U platform by the Ansmann 2447-3035-20-520 14.54V Lithium-Ion rechargeable battery pack and on the 3U-12U platform by the AF-TERTECH eb-18352241414724 V 7 Ah Lithium-Ion rechargeable battery pack. Figure 3.18 shows the fine mass balancing system components mounted on the two ABT platforms.

Given the specification of the mass balancing system components, it is interesting to evaluate the theoretical level of performance achievable from the mass balancing system. Recalling Equation (2.34), the minimum achievable gravity torque on the two platforms can be evaluated as:

Table 3.5: Xsens IMU gyroscope performance

Gyroscope specification	Unit	MTi 1-series
Standard full range	$[\circ/s]$	± 2000
In-run bias stability	$[\circ/h]$	10
Bandwidth (-3dB)	[Hz]	255
Noise density	$[\circ/s/\sqrt{Hz}]$	0.007
g-sensitivity (calibrated)	$[\circ/s/g]$	0.001
Non-linearity	[%FS]	0.1
Scale Factor variation	[%]	0.5 (typical) 1.5 (over life)

Table 3.6: Linear actuators technical specifications

Model name	Travel per step @1.8° [mm]	Stroke length [mm]	Carrier mass [g]
DINGS'28 DLM150	0.0015875	150	18.7
DINGS'42 DLM250	0.0030480	250	84.8

$$\begin{aligned}
\tau_{g,min,1U} &= 18.7 \times 10^{-3} \text{ kg} \cdot 1.5875 \times 10^{-6} \text{ m} \cdot 9.81 \text{ m s}^{-2} \\
&= 2.91 \times 10^{-7} \text{ N m}
\end{aligned} \tag{3.3}$$

$$\begin{aligned}
\tau_{g,min,3U-12U} &= 84.8 \times 10^{-3} \text{ kg} \cdot 3.048 \times 10^{-6} \text{ m} \cdot 9.81 \text{ m s}^{-2} \\
&= 2.5 \times 10^{-6} \text{ N m}
\end{aligned} \tag{3.4}$$

The value obtained should be considered a theoretical ideal value; errors caused by imperfect stepper motor position control (due to backlash, for instance), errors in sensors and imperfect installation (non-orthogonality) will result in a higher residual gravity torque.

Optionally, the shifting masses value can be increased to increase control authority at the cost of a degrade in resolution and achievable residual gravity torque. Figure 3.19 shows the additional mass mounted on the stepper motor carriers.

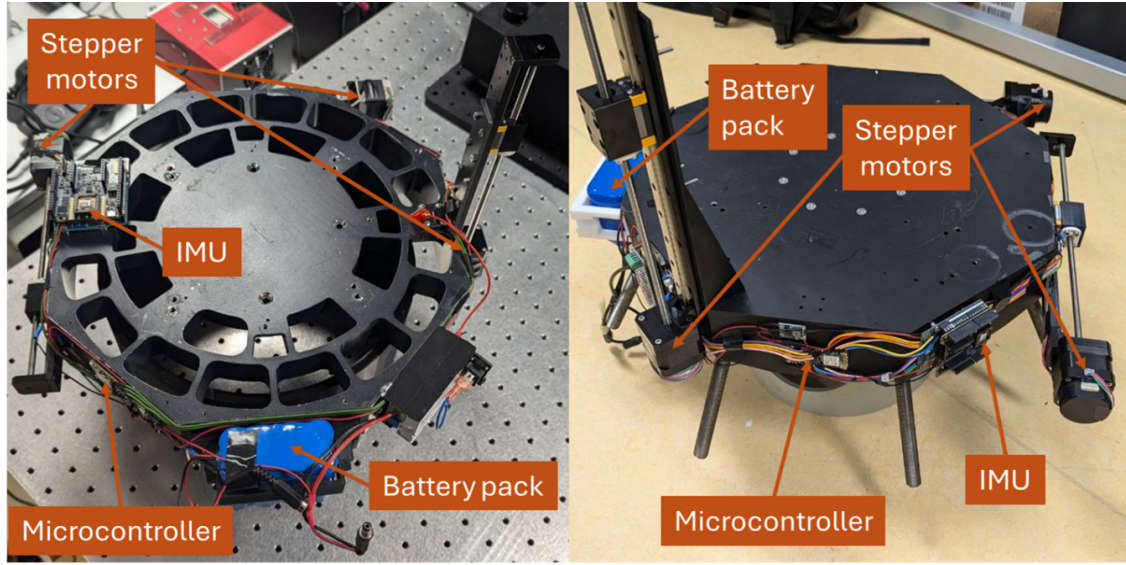


Figure 3.18: The 1U and 3U-12U platforms with highlighted the fine mass balancing system components

3.4.2.2 In-plane mass balancing

In-plane mass balancing consists in reducing the component of the CM to CR offset perpendicular to the gravity vector. The effect of the gravity torque created by this component can be sensed by the on-board IMU: the gravity torque causes the platform to tilt with respect to a condition in which the platform plane is horizontal. When this component of the gravity torque is equal to zero, the platform plane is instead horizontal. Sliding masses can be moved based on the tilt angles until the horizontal orientation is obtained. This process is done online and it is called **automatic mass balancing**.

The control torque obtained by the movement of the fine balancing system masses is described by Equation (2.40), here reported:

$$\boldsymbol{\tau}_u = \Delta \mathbf{r} \times m_b \mathbf{g} \quad (3.5)$$

where $\mathbf{g}^b = \mathbf{g}$ given that the equation is expressed in body frame.

The goal of the balancing system is to design a $\boldsymbol{\tau}_u$ which allows to minimise the CM to CR offset. However, not every control torque $\boldsymbol{\tau}_u$ can be actuated by the balancing system, the control torque will always lie in the plane perpendicular to the gravity vector. The control torque shall be then designed in such a way it can be actuated by the balancing system, this is described in Section 3.4.2.

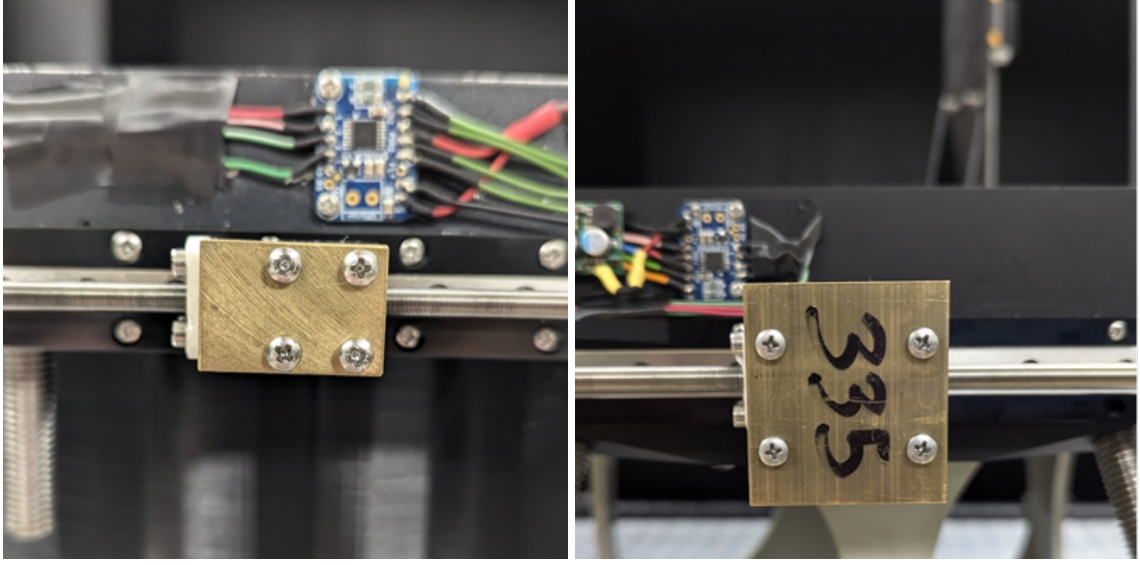


Figure 3.19: Stepper motor mass mounted on the stepper motor carrier of the 1U platform (left) and of the 3U-12U platform (right)

Following [Bahu and Modenini \(2020\)](#), the control torque is computed according to the following nonlinear control law:

$$\boldsymbol{\tau}_u = -k_p \hat{\mathbf{g}} \times \hat{\mathbf{z}} - k_d \boldsymbol{\omega}_p - k_i \int_0^T (\hat{\mathbf{g}} \times \hat{\mathbf{z}}) dt \quad (3.6)$$

where k_p , k_i and k_d are control gains, $\hat{\mathbf{g}}$ and $\hat{\mathbf{z}}$ are respectively the gravity unit vector and the z axis unit vector in body frame, and $\boldsymbol{\omega}_p$ is the projection of the angular velocity in the plane perpendicular to the gravity vector, computed as:

$$\boldsymbol{\omega}_p = \boldsymbol{\omega} - \boldsymbol{\omega}_g \quad \boldsymbol{\omega}_g = \hat{\mathbf{g}}(\hat{\mathbf{g}}^T \boldsymbol{\omega}) \quad (3.7)$$

or equivalently as:

$$\boldsymbol{\omega}_p = \hat{\mathbf{g}} \times (\boldsymbol{\omega} \times \hat{\mathbf{g}}) \quad (3.8)$$

The vector resulting from Equation (3.6) will lie in the plane perpendicular to $\hat{\mathbf{g}}$ by definition, hence it can be actuated from the mass balancing system. Equation (3.5) has a solution in this case given by ([Chesi et al., 2014](#)):

$$\Delta \mathbf{r} = \frac{\hat{\mathbf{g}} \times \boldsymbol{\tau}_u}{\|\mathbf{g}\|^2 m_b} \quad (3.9)$$

The control law described by Equation (3.6) requires an estimation of the platform angular velocity. In case this estimation is affected by high frequency noise, a smoother signal could be obtained by differentiating the gravitational acceleration signal. Starting from Equation (3.7), the angular velocity projection can be derived from the body-frame derivative of $\hat{\mathbf{g}}$ as:

$$\boldsymbol{\omega}_p = \hat{\mathbf{g}} \times (\boldsymbol{\omega} \times \hat{\mathbf{g}}) = -\hat{\mathbf{g}} \times \dot{\hat{\mathbf{g}}} \quad (3.10)$$

where the vector kinematic equation $\dot{\hat{\mathbf{g}}} = -\boldsymbol{\omega} \times \hat{\mathbf{g}}$ has been used. The time derivative of the acceleration vector is calculated on-board with a finite difference. The effectiveness of this solution in reducing the noise in the control torque is evaluated in Section 4.1.1.

3.4.2.3 System identification

Once in-plane balancing is achieved, the residual component of the CM to CR offset is parallel to the gravity vector. This component of the offset cannot be directly sensed by the IMU since it does not produce a tilt angle, given that the platform equilibrium condition is stable. Indeed, if the platform CM is below the CR, the equilibrium point is stable and the platform maintains the horizontal attitude, conversely if the CM is above the CR the equilibrium is unstable and the platform tilts on one side. For this reason, the fine balancing step is performed with the carrier of the stepper motor guide guided with the z axis at its lowest position, to ensure that the CM is below the CR.

The vertical component of the CM to CR offset shall be estimated. Following (Bahu and Modenini, 2020) the problem of estimating the vertical offset and the inertia matrix of the rotating platform can be formulated as a constrained linear Least-Squares (LSQ) problem:

$$\begin{aligned} \hat{H}\mathbf{x} &= \mathbf{0} \\ \text{s.t. : } B\mathbf{x} &= \mathbf{c} \end{aligned} \quad (3.11)$$

where H is the observation matrix, $\mathbf{x}$ is the vector of dynamic parameters to be estimated, matrix B and $\mathbf{c}$ set the constraints for the LSQ problem. The reason why this is a constrained estimation problem and not simply a LSQ problem is reported below.

First, we rearrange Equation (2.39) describing the dynamics of the rotating plat-

form about the CR. If we assume that the only disturbance torque acting on the platform is the gravity torque, we have:

$$J\dot{\omega} = -\omega \times J\omega + \mathbf{r}_{CM,0} \times m_{tot}\mathbf{g} \quad (3.12)$$

Equation (3.12) can be rearranged as (Kim and Agrawal, 2009):

$$\Omega(\dot{\omega})J + \omega \times \Omega(\omega)J = -m_{tot}[\mathbf{g}^\times]\mathbf{r}_{CM,0} \quad (3.13)$$

where Ω is a matrix which rearranges the elements of ω or $\dot{\omega}$ and J is a matrix which rearranges the elements of J :

$$\Omega(\omega) = \begin{bmatrix} \omega_1 & 0 & 0 & 0 & \omega_2 & 0 \\ 0 & \omega_2 & 0 & \omega_1 & 0 & \omega_3 \\ 0 & 0 & \omega_3 & 0 & \omega_1 & \omega_2 \end{bmatrix} \quad (3.14)$$

$$\tilde{J} = [J_{xx} \ J_{yy} \ J_{zz} \ J_{xy} \ J_{xz} \ J_{yz}]^T \quad (3.15)$$

Equation (3.13) can be further rearranged in the following matrix block form:

$$\begin{bmatrix} \Omega(\dot{\omega}) + [\omega^\times]\Omega(\omega) \\ m_{tot}[\mathbf{g}^\times] \end{bmatrix} \begin{bmatrix} \tilde{J} \\ \mathbf{r}_{CM} \end{bmatrix} = \mathbf{0} \quad (3.16)$$

which can be reformulated as a LSQ problem

$$H\mathbf{x} = \mathbf{0} \quad (3.17)$$

where $\mathbf{x} = [\tilde{\mathbf{J}}^T \ \mathbf{r}_{CM}^T]^T = [J_{xx} \ J_{yy} \ J_{zz} \ J_{xy} \ J_{xz} \ J_{yz} \ r_{CM,x} \ r_{CM,y} \ r_{CM,z}]^T$.

If a (known) control torque is applied on the platform, the right-hand sides of Equation (3.16) and Equation (3.17) would have been different from zero and a solution for the unconstrained LSQ problem could have been found. Conversely, if the right hand side is equal to zero, excluding the trivial solution $\mathbf{x} = \mathbf{0}$, all other $\mathbf{x}$ belongs to the kernel of H and differ for a scaling factor. The problem can be solved if we pose some constraints on $\mathbf{x}$ (Equation (3.12)). Following the approach introduced in Bahu and Modenini (2020), we take advantage of the fact that after in-plane balancing, the value of $r_{CM,x}$ and $r_{CM,y}$ is ideally equal to zero. We can impose a known offset to $r_{CM,x}$ and $r_{CM,y}$ by commanding a known value to the shifting masses position Δr_x and Δr_y , the two set of values being

linked by Equation (2.33). Using this approach, we have:

$$B = \begin{bmatrix} \mathbf{0}_{1 \times 6} & 1 & 0 & 0 \\ \mathbf{0}_{1 \times 6} & 0 & 1 & 0 \end{bmatrix}, \quad \mathbf{c} = \begin{bmatrix} r_{CM,x} \\ r_{CM,y} \end{bmatrix} \quad (3.18)$$

Since this approach requires differentiating angular velocity which is noisy, a pre-filtering with a Savitzky–Golay Filter is applied to the data to eliminate high-frequency noise. Once $r_{CM,z}$ is estimated, it can be compensated by shifting the mass on the linear actuator in the z direction.

The inertia matrix can be used to estimate the residual torque, τ_r acting on the rotating platform after balancing as in:

$$\tau_r = J\dot{\omega} = \omega \times J\omega \quad (3.19)$$

Again here differentiation of noisy angular velocity data is involved, a SGF pre-filtering is applied. The residual torque can be then characterised by its Root Mean Square (RMS) and peak value.

3.4.2.4 Fine mass balancing procedure

The accuracy of the inertia and vertical offset estimation depends on the accuracy of the in-plane balancing, since the value of $r_{CM,x}$ and $r_{CM,y}$ is used within the constraint equation. In-plane balancing feedback control uses the gravitational vector in body frame through Equation (3.6). An equal error in the knowledge of the x and y components of the gravitational vector results in a greater residual $r_{CM,x}$ and $r_{CM,y}$ for greater CM to CR vertical offset $r_{CM,z}$, as shown in Figure 3.20 for the x-z plane. Hence, in-plane balancing performed after a compensation of the CM to CR vertical offset will also result in a smaller horizontal CM to CR offset, which in turn improves the estimation part. It is then required to iterate in-plane balancing and vertical offset estimation to improve the accuracy of the algorithm. The iterations are stopped where a specified performance metric does not improve further.

The mass balancing algorithm defined in [Bahu and Modenini \(2020\)](#) has been updated after the mass balancing tests performed at ESTEC ESA centre with the contribution of ESA. The steps of the algorithm are reported here and represented in the diagram of Figure 3.21:

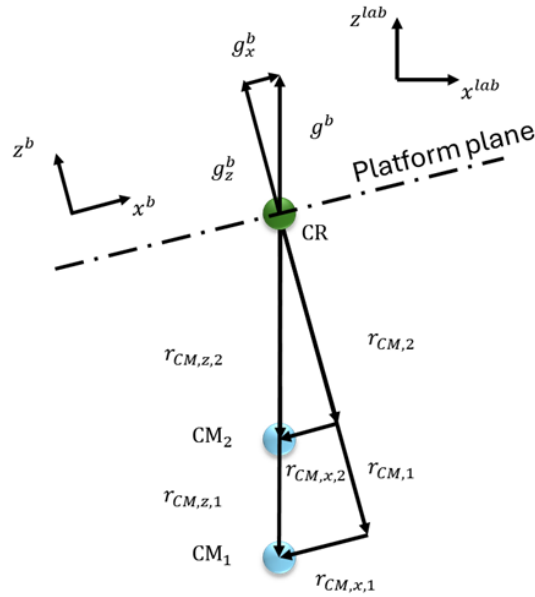


Figure 3.20: The accuracy of in-plane balancing depends on the vertical CM to CR offset. In case 2 the accuracy is improved with respect to case 1

1. Set the vertical stepper motor carrier at its lower position along the linear guide;
2. Perform in-plane balancing;
3. Set an offset for the x and y stepper motors carrier with respect to the position reached after in-plane balancing. Tilt the platform on one side, collect free oscillation data, estimate inertia, and vertical offset;
4. Compensate the vertical offset by moving up the vertical stepper motor carrier;
5. Perform again in-plane balancing to improve performance;
6. Estimate the vertical offset and compensate it by moving up the vertical stepper motor carrier;
7. Repeat steps 5 and 6 until the estimation of the vertical offset does not reduce further;
8. Tilt the platform to one side and let go with minimal residual motion, then observe what happens. Repeat this with a tilt in the other four quadrants;

9. Evaluate if the platform behaviour resembles a stable pendulum, an unstable pendulum, or if it is ambiguous:
 - (a) If it consistently oscillates back toward the horizontal position, it behaves like a stable pendulum. Manually command positive steps to the z-stepper motor until behaviour changes;
 - (b) If it consistently diverges to the pitch/roll range limits, it behaves like an inverted pendulum. Manually command negative steps to the z-stepper motor until behaviour changes;
 - (c) If behaviour is asymmetric across quadrants (i.e., it tends to tilt toward one side), it is horizontally imbalanced. Repeat the procedure from step 5;
 - (d) If it is not clear if the platform behaves like a stable or unstable pendulum, the platform is close to the neutral point (i.e., CM is close to CR), and the gravity torque has been reduced to the level of other disturbance torques. Proceed with step 10.
10. Collect angular velocity data of the platform's free motion and estimate gravity torque;
11. Optionally, the estimation of the gravity torque can be done in the previous steps to assess the improvement of the balancing through the mass balancing algorithm steps.

3.4.2.5 Fine mass balancing software

The fine mass balancing software is mainly composed of two parts: on the platform side, the compiled Arduino software, on the ground station side, the MATLAB mass balancing GUI. The two pieces of software interact together by exchanging packets over the LAN using the UDP protocol. The microcontroller implements a finite state machine to manage operational modes, sensor data acquisition, actuator control and network communication. The different operating modes consist of:

- Connection and initialization mode: establishes the UDP communication with the ground station and initializes the IMU and the motors;

- Calibration mode: computes and offsets the gyroscope bias ;
- Stepper motor positioning mode: moves the stepper motor carriers to a user-specified position;
- Automatic balancing mode: performs the in-plane balancing;
- Telemetry mode: collects data from the IMU and sends it to the ground station; this mode is always active after connection and initialisation, but can be disabled after balancing to save power.

The microcontroller listens continuously for commands from the ground station and triggers different operating modes based on the received message.

The MATLAB balancing software presents an event-based execution; it includes a front-end part (the GUI) and the corresponding back-end. The software functionalities are:

- starting of automatic in-plane balancing;
- setting of gains for the in-plane balancing control;
- commanding of an arbitrary position for the stepper motors;
- collection and saving of telemetry data coming from the platform;
- visualisation in real time of telemetry data: (normalised) gravity vector components, angular velocity, stepper motor carriers position;
- inertia and offset estimation;
- offset estimation;
- torque estimation;
- load and save inertia and offset estimation;
- change of control and communication frequency;
- platform sensor calibration;
- send of custom commands to the microcontroller.

Figure 3.22, Figure 3.23 and Figure 3.24 represent state machine flowcharts for the different on-board balancing software functions linked to the ground station software commands. Boxes in grey represent states of the microcontroller, boxes in orange represent user inputs through the balancing GUI, boxes in blue represent actions of the ground station software, arrows in red represent transitions triggered by the user, while arrows in green represent automatic transitions. A screenshot of the mass balancing GUI with number labelling the different components is shown in Figure 3.25. The labels correspond to the following components of the GUI:

1. Connection controls: Establish connections with the on-board balancing software and send general-purpose commands. The button “Change settings” opens the GUI;
2. In-plane balancing controls: Set parameters and recall functions for the in-plane automatic mass balancing;
3. Stepper motor position controls: Set a user-chosen position for the stepper motors’ carriers and check its value in steps and mm;
4. System identification control: Recall functions for system identification and set SGF filter parameters;
5. Gyroscope calibration: Calibrate the on-board IMU gyroscope;
6. Telemetry acquisition control: Set parameters relative to telemetry acquisition, collect, and save telemetry for system identification and experiment data logging;
7. Telemetry visualisation: Visualise numerical values of telemetry and time in live-updating plots.

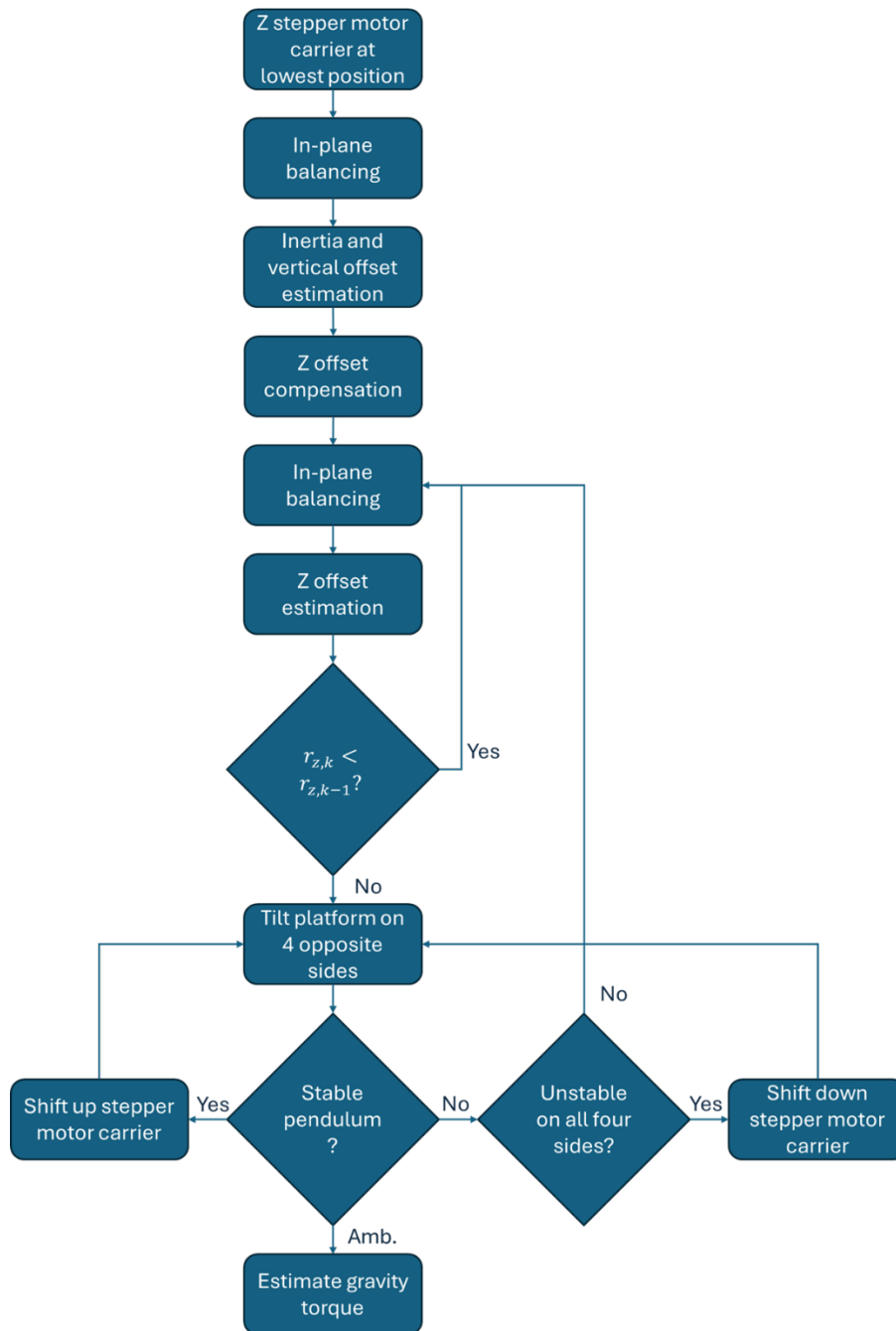


Figure 3.21: Fine mass balancing algorithm

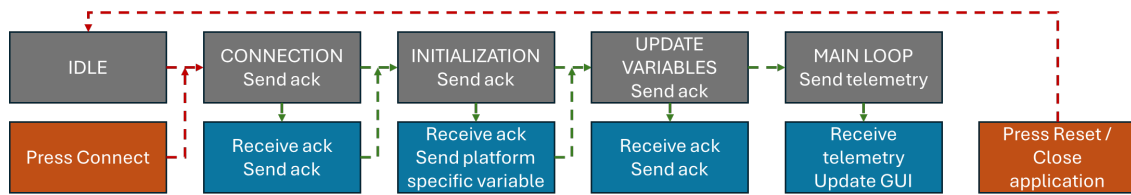


Figure 3.22: State machine flowchart for connection, initialization and reset of the on-board balancing software

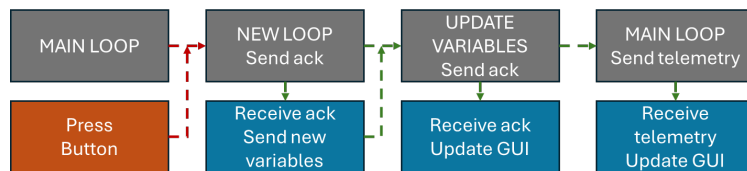


Figure 3.23: State machine flowchart for the status update, gyroscopes calibration, gain updates, stepper motor position update functions of the on-board balancing software

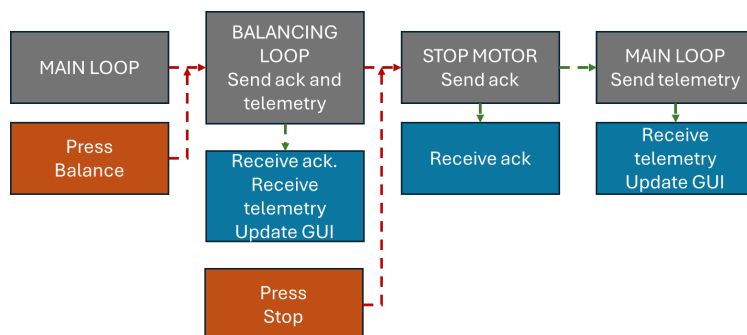


Figure 3.24: State machine flowchart for the balancing function of the on-board balancing software

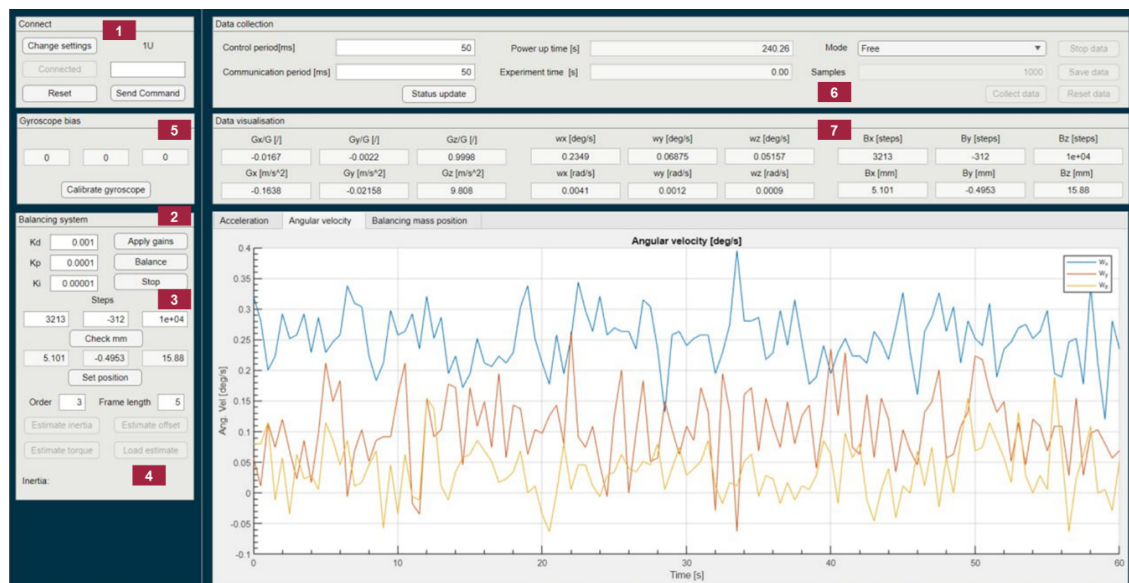


Figure 3.25: CubeDynA mass balancing GUI

3.4.2.6 Mixed magnetic/mechanical balancing

Most content of this section has been published as a Technical Note on the MDPI Aerospace journal under the title of Automatic Balancing for Satellite Simulators with Mixed Mechanical and Magnetic Actuation (Curatolo et al., 2022). The content report in this section contains minor wording edits with respect to the original work.

The fine balancing process requires knowledge of angular velocity, when these measurements come from the IMU gyroscope they are affected by high frequency noise. On the measurement side, a solution as the one proposed in Section 3.4.2.2 can be adopted, where the angular velocity is estimated by a less noisy measurement such as the one of the gravity vector. On the actuator side, instead, a smoother form of actuation can be utilised. If high-frequency noise from gyros enters the closed loop system indeed, it could result in unwanted mechanical vibration that, in turn, can increase gyro noise amplitude. A combined mechanical/magnetic actuation for instance could pose a viable alternative when magnetic control is available. The main idea is to distribute the required control between two kinds of actuators. The proportional and integral parts are realised by the stepper motors, which move a small mass, while the derivative/damping part is realised by the magnetic torque generated thanks to magnetic actuation. This can result in a smoother positioning for the sliding masses and, in turn, a more effective imbalance reduction. The modifications required by the proposed balancing technique with respect to a standard approach based only on shifting masses are minimal, provided that the testbed is equipped with a magnetic field simulator.

This balancing approach has been verified on a heritage ADCS facility of the University of Bologna. This facility includes a Helmholtz cage and a ADCS mock-up with the same electronic components used for the mass balancing as in the Cube-DynA facility, except for the stepper motor models. The ABT platform additionally includes three magnetorquers and one reaction wheel to emulate the ADCS of a nanosatellite. The tabletop platform, the mechanics and electronics of the balancing system. The mock-up is equipped with a set of three mutually orthogonal magnetorquers, capable of generating 1 Am² of magnetic dipole each, and with the Adafruit Feather M0 microcontroller (item a, following the Figure 3.26 nomenclature) that communicates through I2C with the XSens IMU (b) and an

Adafruit Motor FeatherWing board (c). The FeatherWing board drives the magnetorquers (d.1, d.2, d.3). Three stepper motors (e.1, e.2, e.3) are commanded by stepper drivers (f) that are controlled by the M0 microcontroller. Each motor moves a mass of 0.041 Kg (g.1, g.2, g.3) with a minimum step of 2 μm . The IMU and the M0 microcontroller are powered by a 3.6 V Lithium-Ion (LiIO) battery (h), while a 7.2 V LiIO battery (i) powers the three stepper motors and the three magnetorquers.

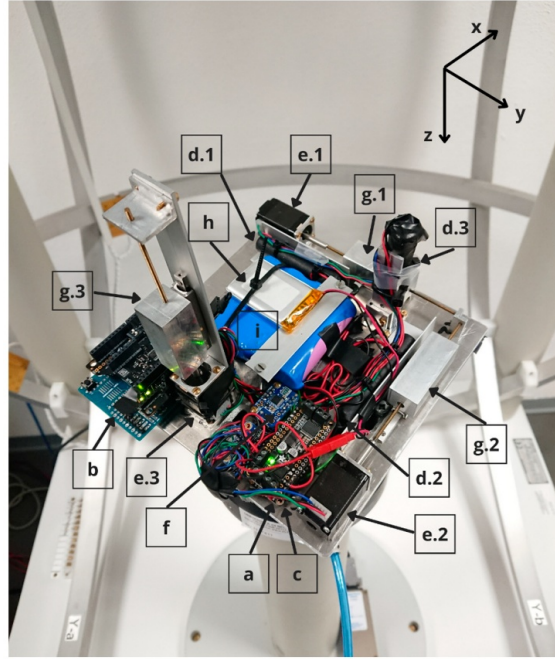


Figure 3.26: The ADCS mock-up of the heritage air bearing facility of the University of Bologna (Curatolo et al., 2022). See text for label explanation.

The rotating platform dynamics is described again by Equation (2.39) here reported:

$$J\dot{\omega} = -\omega \times J\omega + \mathbf{r}_{CM,0} \times m_{tot}\mathbf{g}^b + \boldsymbol{\tau}_u \quad (3.20)$$

In this application the control torque $\boldsymbol{\tau}_u$ is realised by two contributions, the mechanical torque $\boldsymbol{\tau}_{sm}$ and the magnetic torque $\boldsymbol{\tau}_{mag}$:

$$\boldsymbol{\tau}_u = \boldsymbol{\tau}_{sm} + \boldsymbol{\tau}_{mag} = \mathbf{r}_b \times m_b\mathbf{g} + \mathbf{d} \times \mathbf{b} \quad (3.21)$$

where m_b is the sliding mass, $\mathbf{r}_b$ is the mass displacement vector in body axes,

$\mathbf{d}$ is the dipole vector created by the set of magnetorquers, and $\mathbf{b}$ is the external magnetic field vector, assumed to be controllable (which is the case in our simulator thanks to a Helmholtz cage). In the following, we will assume the external magnetic field $\mathbf{b}$ to be anti-parallel to the local vertical (i.e., positive upward). The feedback control law is the same of Equation (3.6), here reported:

$$\boldsymbol{\tau}_u = -k_p \hat{\mathbf{g}} \times \hat{\mathbf{z}} - k_d \boldsymbol{\omega}_p - k_i \int_0^T (\hat{\mathbf{g}} \times \hat{\mathbf{z}}) dt \quad (3.22)$$

The proportional and integral part are actuated by the sliding masses torque:

$\boldsymbol{\tau}_{sm}$:

$$\boldsymbol{\tau}_{sm} = -k_p \hat{\mathbf{g}} \times \hat{\mathbf{z}} - k_i \int_0^T (\hat{\mathbf{g}} \times \hat{\mathbf{z}}) dt \quad (3.23)$$

and the mass displacement is computed with Equation (3.9), here reported:

$$\Delta \mathbf{r} = \frac{\mathbf{g} \times \boldsymbol{\tau}_u}{\|\mathbf{g}\|^2 m_b} \quad (3.24)$$

The derivative part is actuated by the magnetic torque:

$$\boldsymbol{\tau}_{mag} = -K_d \boldsymbol{\omega}_\perp \quad (3.25)$$

which is obtained by setting the magnetic dipole $\mathbf{d}$ equal to:

$$\mathbf{d} = \frac{1}{b} \hat{\mathbf{b}} \times \boldsymbol{\tau}_{mag} = \frac{K_d}{b} \hat{\mathbf{g}} \times \boldsymbol{\omega} \quad (3.26)$$

where the rightmost side has been derived upon substitution of Equation (3.25), noticing that:

$$\hat{\mathbf{g}} \times \boldsymbol{\omega}_\perp = \hat{\mathbf{g}} \times \boldsymbol{\omega}.$$

The in-plane balancing method based on the use of mechanical and magnetic mixed actuation is compared to the in-plane balancing method that makes use of mechanical actuation only. The two methods are compared in terms of residual gravity torque and steady-state RMS value of the normalised gravity vector x and y components, which should be ideally zero when in-plane balancing is completed. As a result, the magnitude of the torque acting on the platform after balancing is about 14% smaller on average in the mixed magnetic/mechanical case compared to the solely mechanical case (6.95×10^{-5} N m vs 8.10×10^{-5} N m). The magnitude of the in-plane component of the gravity acceleration RMS is smaller by about 45% when adopting the new method compared to the classical

mechanical one (2.88×10^{-3} vs 5.53×10^{-3}). Figure 3.27 shows a comparison of the gravity acceleration in the two cases. The peak-to-peak variation of the stepper motor positions once the steady state is reached is also reduced. Figure 3.28 shows the comparison of the stepper motor carrier position in the two cases, while Figure 3.29 shows the magnetic dipole during mixed mechanical/magnetic balancing.

The use of mixed mechanical/magnetic actuation is able to reduce the mechanical vibrations during in-plane balancing and improve its performance compared to the case in which only mechanical actuation is used. This actuation strategy can be implemented wherever magnetic actuation is available (for instance, in a facility with a Helmholtz cage and magnetic actuators). The CubeDynA facility includes a Helmholtz cage but does not directly include magnetic actuators. Nevertheless, it can host magnetic actuation either as part of a DUT ADCS or as additional GSE to be placed on the ABT. Hence, mixed mechanical/magnetic actuation is an additional tool to be added to the facility to further improve disturbance torque mitigation and simulation fidelity.

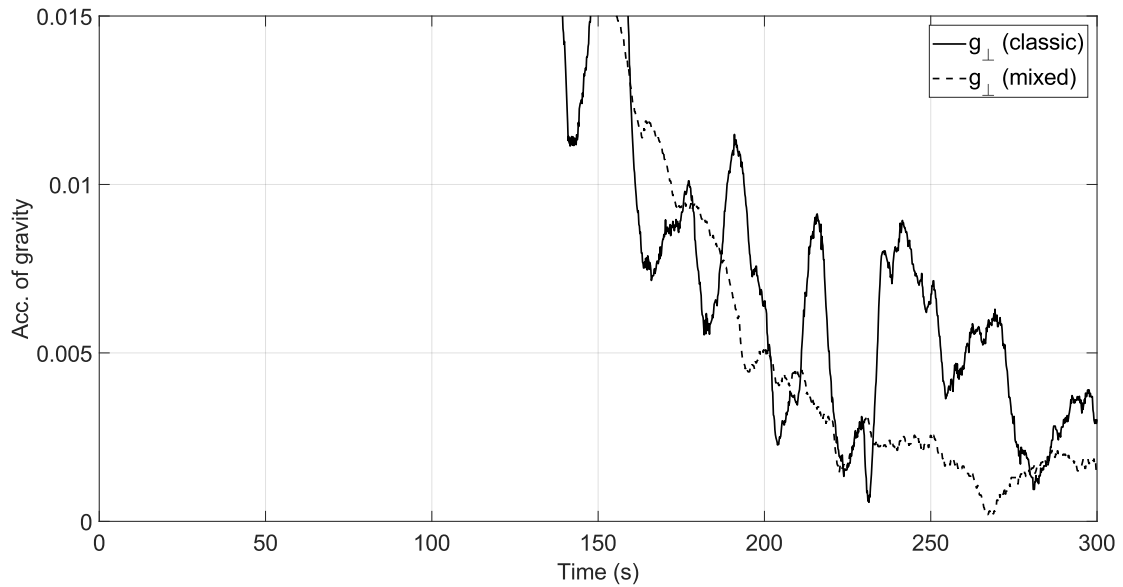


Figure 3.27: Comparison of the projection in the x-y plane of the normalized gravity acceleration in the case of classic balancing (solid line) and mixed balancing (dashed line) (Curatolo et al., 2022).

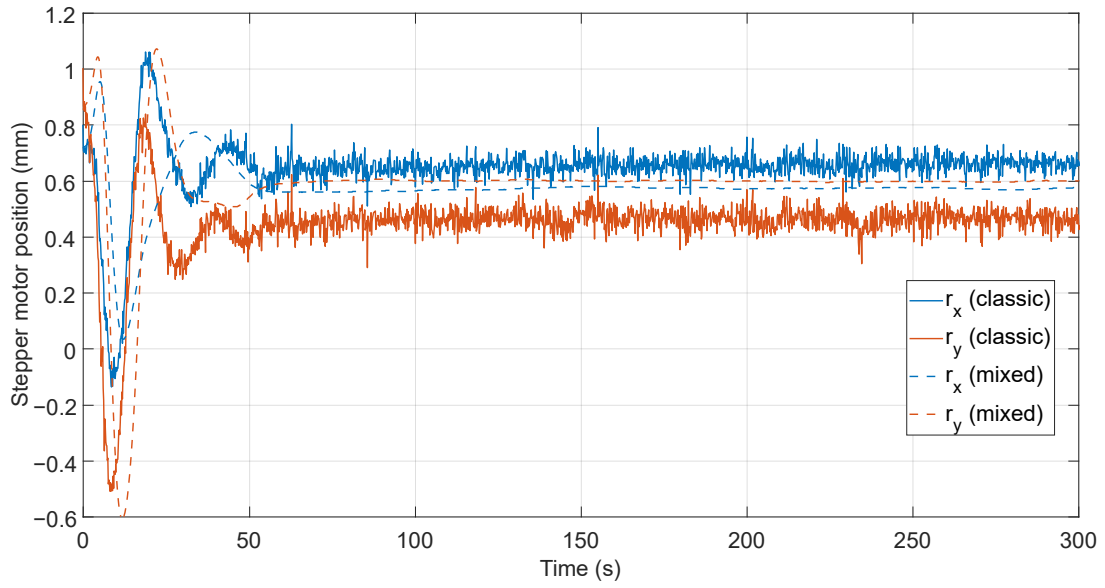


Figure 3.28: Comparison of the sliding masses' position in the case of classic balancing (solid line) and mixed balancing (dashed line) (Curatolo et al., 2022).

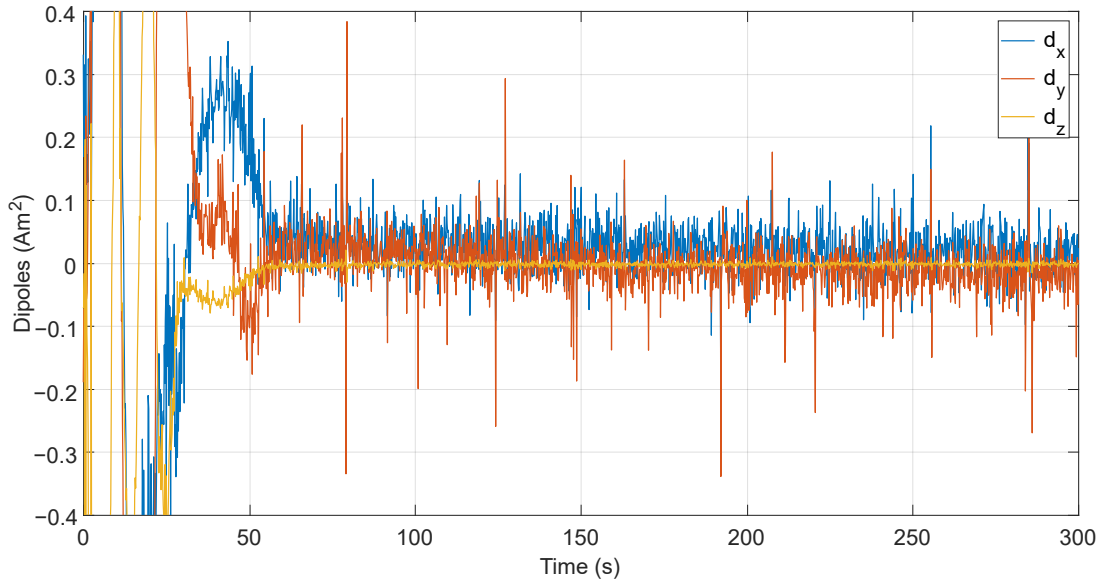


Figure 3.29: Magnetorquers dipole during mixed balancing (Curatolo et al., 2022).

3.5 Communication module

The ADCS often relies on the exchange of telemetry and telecommand between the spacecraft and ground station. It is useful to recreate such functionality in ADCS facilities. Some of the ADCS mock-ups, such as the Spaceinventor, use cabled connection to communicate with the ground. The use of cables is incompatible with an ADCS HIL testing facility, since the motion will be constrained by the cable and the level of disturbance torque would consequently be high. Requirement AOC-TB-34 specifies that a communication module shall be present on the ABT, which connects to the DUT via CAN or I2C and acts as:

1. bi-directional TMTC relay with the ESA/customer-supplied ground station via WiFi;
2. one-directional relay of star tracker template model outputs to the CubeSat or optical stimulator.

Moreover, it should allow for requirement AOC-TB-32 on synchronisation between on-board satellite time and ground station time to be satisfied, by maintaining a delay in packet delivery below 0.1s. The communication is realised by a Raspberry Pi Zero W equipped with the WAVESHARE RPI-485CAN CAN HAT, shown in Figure 3.30. The communication module runs the Raspberry Pi Operating System (OS) which is based on a Linux distribution. The communication module data relay software consists of a compiled C program, which makes use of CSP ([libcsp authors, 2023a](#)) and SPP libraries ([Libre Space Foundation, 2023](#)). The program is set to run as a daemon at startup, in such a way that no user intervention is needed for nominal operations. Requirement AOC-TB-34 specifies the adoption of the CSP for the CAN communication interface. The baseline was to use the same protocol for the I2C interface, but the CSP protocol has not been tested with the I2C interface ([yashi, 2023](#)). For this reason, when the I2C interface is used, communication to the DUT is provided through the Raspberry Pi pigpio open source library ([Joan, Abyz, 2023](#)) and data is formatted as a SPP packet. The main limitation of the library is that it allows to transfer only 16 bytes with a First-In First-Out (FIFO) buffer ([Broadcom Corporation, 2012](#)), forcing a fragmentation and defragmentation of the packet. For this scope, the SPP packet sequence control field of the SPP header is used in the fragmentation and

defragmentation process, which is implemented in the communication module software. The fragmentation operation and its sending are implemented with some microseconds delays in order not to make FIFO overflow. The CAN communication relies on the socketcan drivers and it is fully implemented within the CSP framework.

All CSP packets are sent in a connectionless mode for efficiency. The CSP requires that a Cyclic Redundancy Check (CRC)³² checksum is attached to the packets when this mode is used. Figure 3.31 shows a schematic of the communication routes. The user should specify the IP address and UDP port of the communication module (for WiFi communication) and the CSP destination address and destination port of the packet. The program consists of the two following threads:

- the server thread which listens for incoming packets and executes callback functions;
- the CSP router thread for CSP packets routing;

The two following callback functions are bind to the UDP and CAN interfaces port:

- *managePacketUDP(csp_packet_t * packet)* bound to the communication module UDP interface CSP port: when a CSP packet is received on the UDP interface CSP port (from the facility Ground Station (GS) or ESA/Customer GS), the packet is unpacked, packed again as a SPP packet and forwarded to the DUT via I2C. Since the I2C works in a master-slave mode, the communication module, which acts as master, sends a request for reading a packet from the DUT. In the case a SPP packet is received, it is unpacked and packed again as a CSP packet and forwarded again back to the ESA/Customer GS.
- *managePacketCAN(csp_packet_t * packet)* bound to the communication module CAN interface CSP port: when a packet is received on the CAN interface CSP port (from the DUT), this is forwarded to the ESA/Customer GS

The communication module shall also ensure that the delay between the generation of the data from the facility GS and its arrival on the DUT does not exceed

0.1 seconds for time synchronisation. The total delivery time is the sum of different contributions from the generation of the packet on the transmitting device to the delivery on the receiving device, passing through the communication module. This path delay time should be measured to ensure it is within requirements.

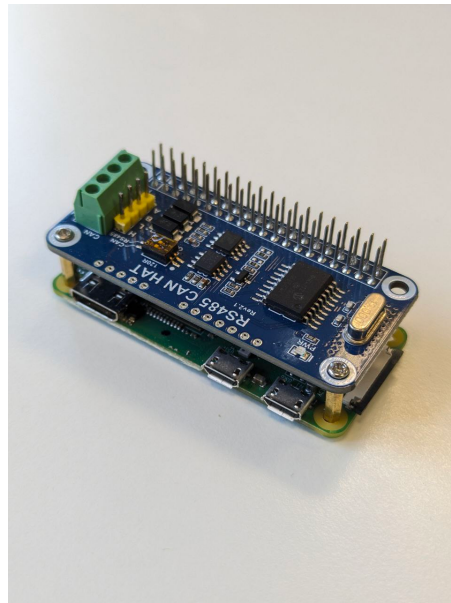


Figure 3.30: CubeDynA communication module: Raspberry Pi Zero W with CAN HAT

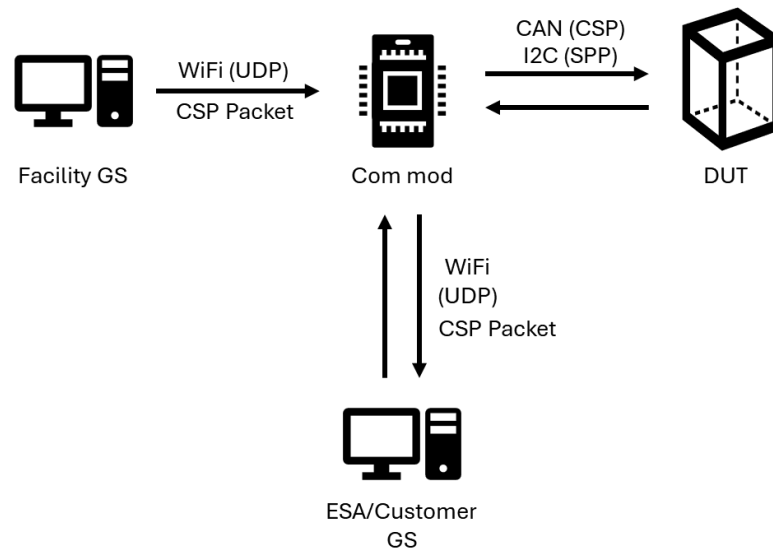


Figure 3.31: Schematics of the communication module functionalities

4

Results of facility commissioning and performance

The results of the facility commissioning are presented in this chapter. Different tests were conducted to assess the performance of the facility and compare it with the requirements. Section 4.1 presents the results of the mass balancing system testing. Section 4.3 presents the results of the communication module testing.

4.1 Mass balancing

The ability of the mass balancing system to reduce the gravity torque level when testing different sizes of CubeSat was verified. Section 4.1.1 reports the improvements in mass balancing obtained by estimating the angular velocity from the gravity vector. Section 4.1.2 reports the results of the coarse mass balancing for CubeSats of size 1U, 3U, 6U and 12U, while Section 4.1.3 reports the results of the fine balancing for the same set of CubeSats. The facility residual disturbance torque is estimated. Finally, Section 4.3 reports the results of the communication module testing.

4.1.1 Estimation of angular velocity from the gravity vector

The projection in the horizontal plane of the angular velocity using the gravity vector is compared to the projection obtained using angular rate data from the IMU. The effect on the in-plane balancing is evaluated.

The gravity vector is obtained by the quaternion output from the Xsens IMU sensor fusion algorithm. Recalling Equation (2.10), the gravity vector in body frame can be obtained as:

$$\hat{\mathbf{g}} = \begin{bmatrix} 1 - 2(q_2^2 + q_3^2) & 2(q_1q_2 + q_3q_0) & 2(q_1q_3 - q_2q_0) \\ 2(q_1q_2 - q_3q_0) & 1 - 2(q_1^2 + q_3^2) & 2(q_2q_3 + q_1q_0) \\ 2(q_1q_3 + q_2q_0) & 2(q_2q_3 - q_1q_0) & 1 - 2(q_1^2 + q_2^2) \end{bmatrix} \begin{bmatrix} 0 \\ 0 \\ 1 \end{bmatrix} = \begin{bmatrix} 2(q_1q_3 - q_2q_0) \\ 2(q_2q_3 + q_1q_0) \\ 1 - 2(q_1^2 + q_2^2) \end{bmatrix} \quad (4.1)$$

where $[0 \ 0 \ 1]^T$ is the normalised gravity vector in inertial frame, considered positive upward (this is done to be coherent with the IMU axes convention, the balancing control law does not change because of this).

In the case in which the angular velocity reading is used to obtain its projection in the horizontal plane, this is calculated using Equation (3.8) here reported:

$$\omega_p = \hat{\mathbf{g}} \times (\boldsymbol{\omega} \times \hat{\mathbf{g}}) \quad (4.2)$$

In the case instead in which the derivative of the gravity vector is used, Equation (3.10) holds, here reported:

$$\omega_p = -\hat{\mathbf{g}} \times \dot{\hat{\mathbf{g}}} \quad (4.3)$$

The derivative of the gravity vector at step k is calculated on-board using backward finite difference involving step k and step $k-1$:

$$\dot{\hat{\mathbf{g}}} = \frac{\hat{\mathbf{g}}(k) - \hat{\mathbf{g}}(k-1)}{\Delta T} \quad (4.4)$$

where ΔT is the time step. The comparison of the two evaluation method is shown in Figure 4.1 (x axis) and in Figure 4.2 (y axis) for the same in-plane balancing experiment on the 1U platform. Table 4.1 instead reports the RMS value of the two signals for the last 30 seconds of the experiment, for which the platform is still.

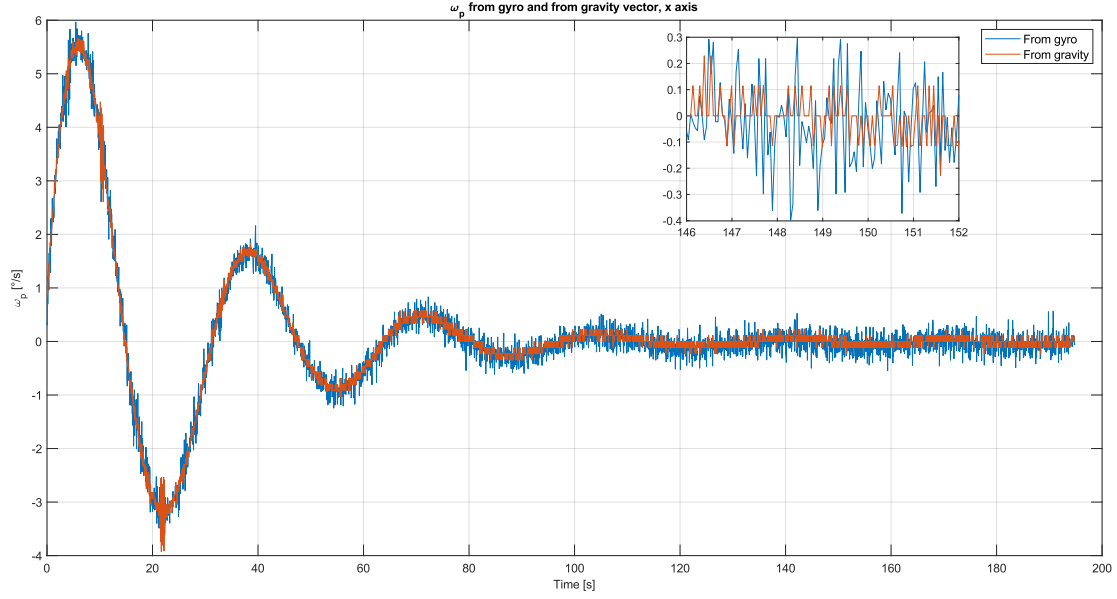


Figure 4.1: Comparison of estimation of angular velocity projection from gyroscope and from gravity vector, x axis

Table 4.1: Comparison of estimation of angular velocity projection from gyroscope and from gravity vector, gravity vector RMS values

Estimation of ω_p	g_x RMS [$^{\circ}/s$]	g_y RMS [$^{\circ}/s$]
$\omega_p = \hat{g} \times (\omega \times \hat{g})$	0.16	0.21
$\omega_p = -\hat{g} \times \dot{\hat{g}}$	0.08	0.08

The two methods have also been compared in terms of resulting stepper motor position in case only the derivative/damping term is present in the in-plane balancing control law. Since this is calculated on-board starting from just one signal, the comparison has been done with two different experiments, in a quasi-steady state of the rotating platform. The comparison of the two evaluation methods is shown in Figure 4.3 (x axis) and in Figure 4.4 (y axis) for two different experiments on the 1U platform. Table 4.2 reports the RMS value for the two signals.

As a result of this new estimation method of the angular velocity projection, the in-plane angular velocity projection RMS during quasi steady state is reduced of about 57 %, while the x and y stepper motor position RMS of about 60 %. The estimation done using the gravity vector is hence implemented in the CubeDynA facility balancing software.

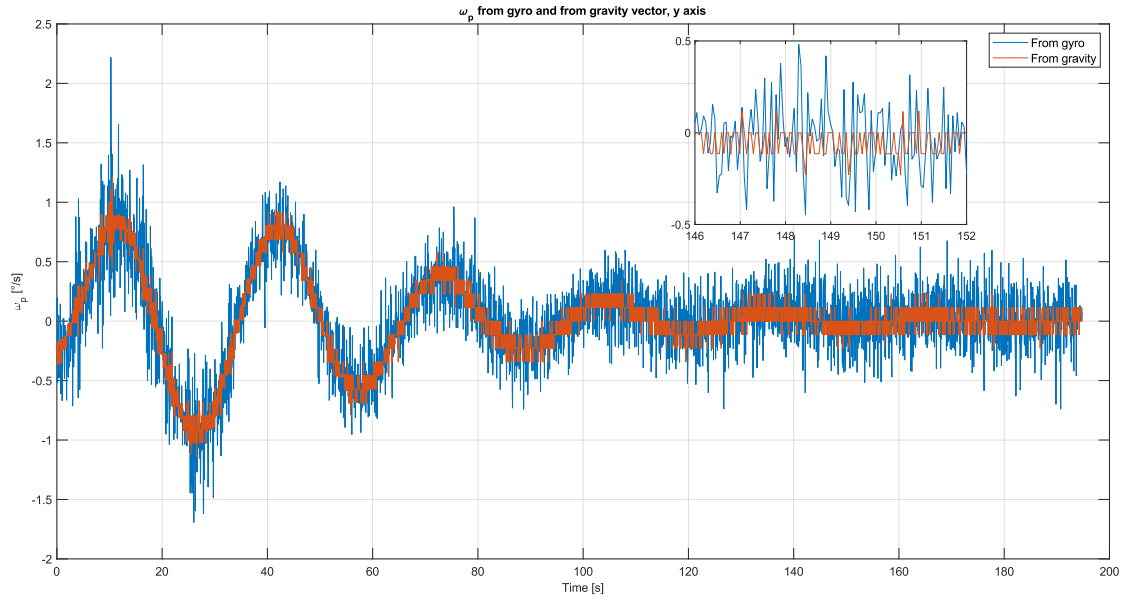


Figure 4.2: Comparison of estimation of angular velocity projection from gyroscope and from gravity vector, y axis

Table 4.2: Comparison of estimation of angular velocity projection from gyroscope and from gravity vector, stepper motor position RMS values

Estimation of ω_p	Δr_x RMS [steps]	Δr_y RMS [steps]
$\omega_p = \hat{\mathbf{g}} \times (\boldsymbol{\omega} \times \hat{\mathbf{g}})$	53	41
$\omega_p = -\hat{\mathbf{g}} \times \dot{\hat{\mathbf{g}}}$	18	20

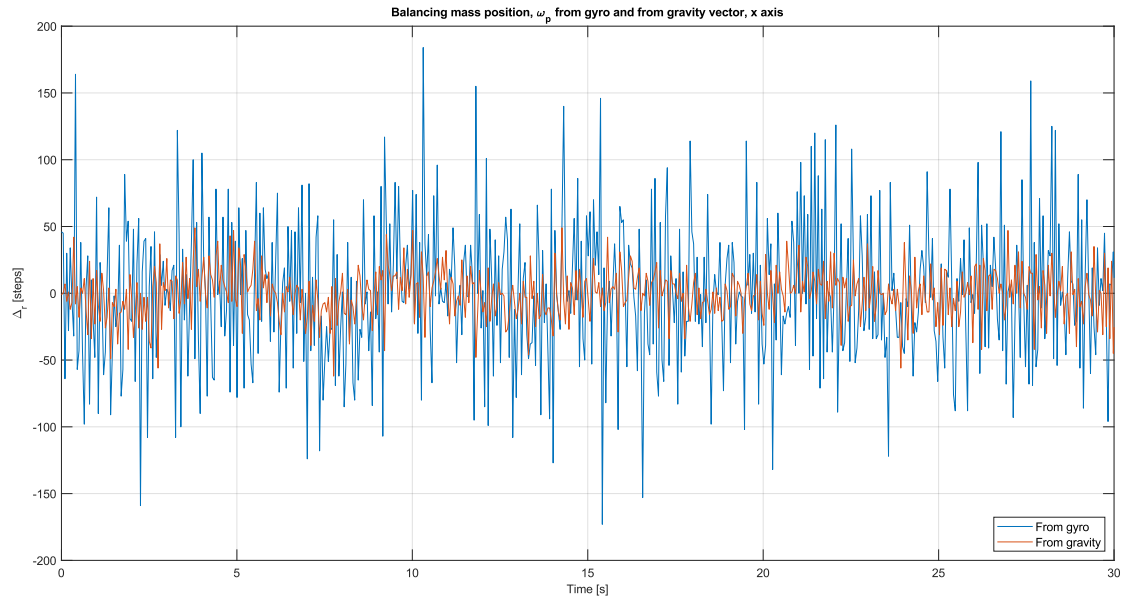


Figure 4.3: Comparison of stepper motor position for the two different estimation method of projected angular velocity, x axis

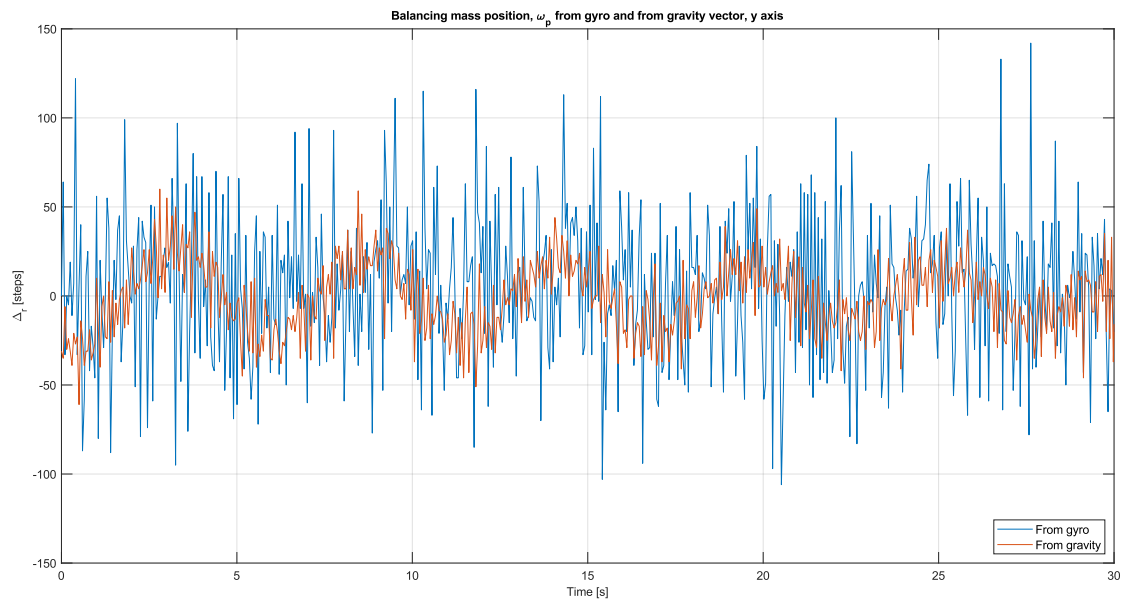


Figure 4.4: Comparison of stepper motor position for the two different estimation method of projected angular velocity, y axis

4.1.2 Results of coarse mass balancing

The testing of the coarse mass balancing is part of the test procedures performed to validate the CubeDynA facility mass balancing system against requirements. The success criteria consist in the ability of the coarse balancing procedure to bring the CM at a distance of the CR that can be compensated by the fine balancing system. This is verified by completing all the steps of the decision diagram of Figure 3.16. The tests have been executed at the University of Bologna facility, during the development period of the CubeDynA facility.

Coarse mass balancing has been tested with CubeSat mock-ups with mass and size compliant with the 1U, 3U, 6U and 12U standard. While the original plan was to test all the cases presented in Table 3.2, time constraints required to test a subset of these cases: for the 1U and the 3U CubeSat the CM position was set respectively to be at $+2\text{ cm}$ and $+7\text{ cm}$ from their GC, for the 6U and 12U CubeSat the CM position is instead coincident with their GC. All the CubeSats have been tested in vertical orientation, i.e. with their z axis aligned with the platform z axis. Overall, all the tests resulted to be successful, i.e. coarse balancing could be achieved for all sizes of CubeSats.

4.1.2.1 1U CubeSat

For the mass balancing of the 1U CubeSat it is assumed that the CubeSat mass is equal to 2 kg and its CM is at a distance of its GC of 2 cm along the $+z$ axis direction. The computation of the coarse balancing mass is shown in Figure 4.5. Since no GSE compliant with the holes pattern on the 1U platform was available at the time of the test, it was decided to increase the mass of the 1U CubeSat mock-up and to shift its CM position to obtain an equivalent contribution to the total moment. The updated coarse mass balancing spreadsheet result is shown in Figure 4.6. The resulting coarse balancing mass is equal to 1.302 kg , a few iterations of the coarse balancing resulted in a slightly lower mass, equal to 1.140 kg . The total mass of the CubeSat and rotating platform is equal to 9.027 kg . The 1U CubeSat mock-up on the testing platform is shown in Figure 4.7.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]	Spacer type	Spacer thickness [mm]
CubeSat 1U	2000	76.75	DUT base	180100	No spacer	0.00
GSE 1	2000	20.00	Platform plane	46600		
GSE 2	0	0.00	Platform plane	0	DUT orientation	Distance of DUT base from platform plane [mm]
1U Platform	3865	-22.06	Platform plane	-72517	1U vertical	10
Air bearing	1444	-33.11	Air bearing top	-47811		
Counterweights total	1302	-35	Platform bottom	-106372		
Counterweights/8	163					
Total mass	10611		SUM	0		

Figure 4.5: Results of coarse mass balancing for the 1U CubeSat with additional GSE

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]	Spacer type	Spacer thickness [mm]
CubeSat 1U	2578	74.64	DUT base	226709	No spacer	0.00
GSE 1	0	0.00	Platform plane	0		
GSE 2	0	0.00	Platform plane	0	DUT orientation	Distance of DUT base from platform plane [mm]
1U Platform	3865	-22.06	Platform plane	-72517	1U vertical	10
Air bearing	1444	-33.11	Air bearing top	-47811		
Counterweights total	1302	-35	Platform bottom	-106381		
Counterweights/8	163					
Total mass	9189		SUM	0		

Figure 4.6: Results of coarse mass balancing for the 1U CubeSat with increased mass to simulate additional GSE contribution

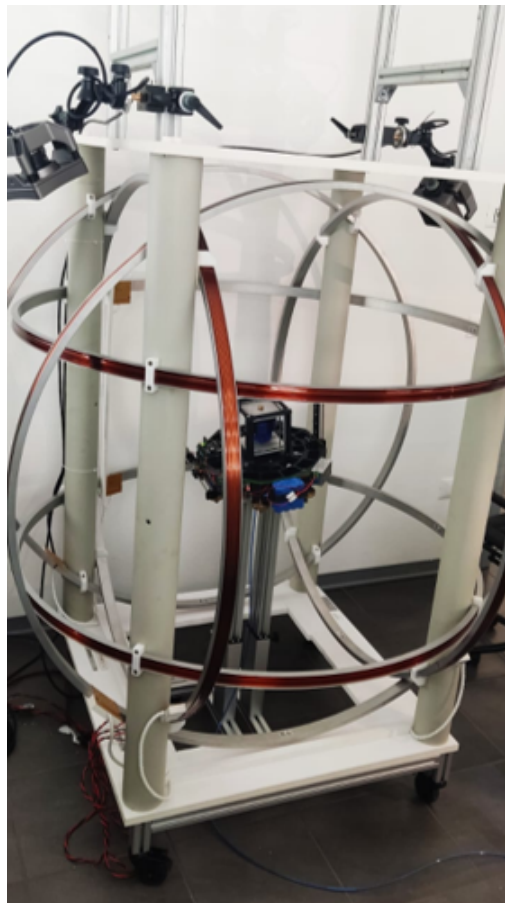


Figure 4.7: 1U CubeSat mock-up on the testing platform

4.1.2.2 3U CubeSat

For the mass balancing of the 3U CubeSat, it is assumed that the CubeSat mass is equal to 6 kg and its CM is at a distance of 7 cm from its GC along the $+z$ axis direction. The addition of GSE has been simulated with dummy weights, with mass equal to 1.652 kg and with a distance from the platform plane of 25.27 mm. This is equivalent to placing GSE of 2 kg with a distance from the platform plane of 20 mm. The balancing spreadsheet results are shown in Figure 4.8; the resulting coarse balancing mass is equal to 10.308 kg. A few iterations of the coarse balancing resulted in a slightly lower mass, equal to 10.272 kg. The total mass of the CubeSat and rotating platform is equal to 30.721 kg. The 3U CubeSat mock-up on the testing platform is shown in Figure 4.9.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]	Spacer type	Spacer thickness [mm]
CubeSat 3U	6000	240.25	DUT base	1531500	No spacer	0.00
GSE 1	1652	25.27	Platform plane	50006		
GSE 2	0	0.00	Platform plane	0		
3U-12U Platform	11353	-15.12	Platform plane	-114892		
Air bearing	1444	-33.11	Air bearing top	-47811		
Counterweights total	10272	-88.54	Platform bottom*	-1418803		
Counterweights/8	1284					
Total mass	30721					

Figure 4.8: Results of coarse mass balancing for the 3U CubeSat

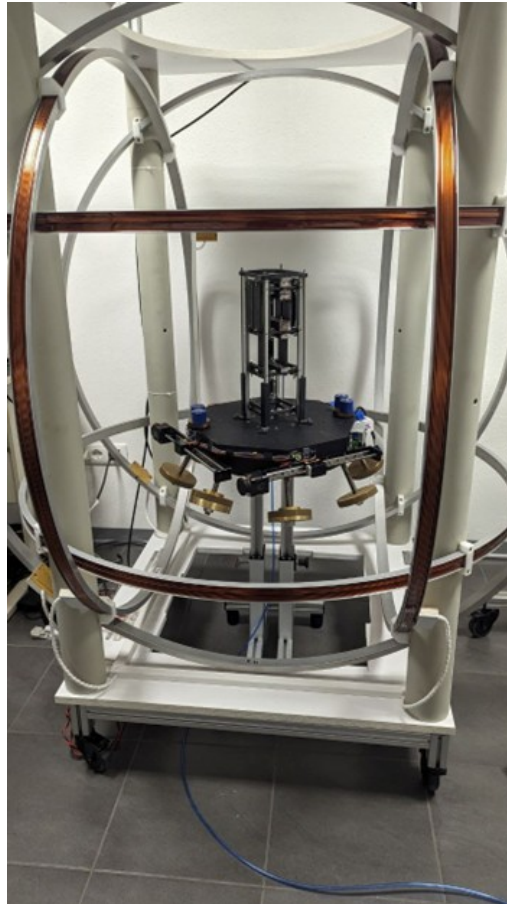


Figure 4.9: 3U CubeSat mock-up on the testing platform

4.1.2.3 6U CubeSat

For the mass balancing of the 6U CubeSat, it is assumed that the CubeSat mass is equal to 12 kg and its CM is coincident with its GC. The addition of GSE has been simulated with dummy weights as in the 3U case. The balancing spreadsheet results are shown in Figure 4.10; the resulting coarse balancing mass is equal to 17.792 kg. A few iterations of the coarse balancing resulted in a slightly lower mass, equal to 17.718 kg. The total mass of the CubeSat and rotating platform is equal to 44.167 kg. The 6U CubeSat mock-up on the testing platform is shown in Figure 4.11.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]	Spacer type	Spacer thickness [mm]
CubeSat 6U	12000	183.00	DUT base	2436000	No spacer	0.00
GSE 1	1652	25.27	Platform plane	50006		
GSE 2	0	0.00	Platform plane	0	DUT orientation	Distance of DUT base from platform plane [mm]
3U-12U Platform	11353	-15.12	Platform plane	-114892	6U/12U vertical	15
Air bearing	1444	-33.11	Air bearing top	-47811		
Counterweights total	17792	-80	Platform bottom*	-2323303		
Counterweights/8	2224					
Total mass	44241					

Figure 4.10: Results of coarse mass balancing for the 6U CubeSat



Figure 4.11: 6U CubeSat mock-up on the testing platform

4.1.2.4 12U CubeSat

For the mass balancing of the 12U CubeSat, it is assumed that the CubeSat mass is equal to 24 kg and its CM is coincident with its GC. The addition of GSE has been simulated with dummy weights as in the 3U and 6U cases. For the 12U balancing, a different set of threaded pins was used, of longer lengths with respect to the standard set. This is equivalent to the solution presented in Figure 3.15 of Section 3.4.1, where the threaded pin length is obtained with a disk of greater

length. The balancing spreadsheet results are shown in Figure 4.12; the resulting coarse balancing mass is equal to 30.648 kg. A few iterations of the coarse balancing resulted in a slightly lower mass, equal to 30.552 kg. The total mass of the CubeSat and rotating platform is equal to 69.001 kg. The 12U CubeSat mock-up on the testing platform is shown in Figure 4.13.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]	Spacer type	Spacer thickness [mm]
CubeSat 12U	24000	183.00	DUT base	4872000	No spacer	0.00
GSE 1	1652	25.27	Platform plane	50006		
GSE 2	0	0.00	Platform plane	0	DUT orientation	Distance of DUT base from platform plane [mm]
3U-12U Platform	11353	-15.12	Platform plane	-114892	6U/12U vertical	15
Air bearing	1444	-33.11	Air bearing top	-47811		
Counterweights total	30648	-108	Platform bottom*	-4759303		
Counterweights/8	3831					
Total mass	69097					

Figure 4.12: Results of coarse mass balancing for the 12U CubeSat

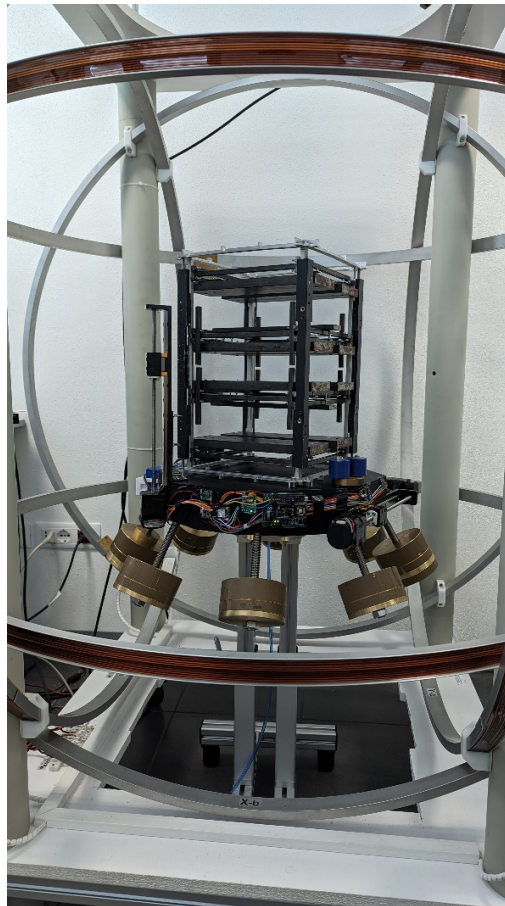


Figure 4.13: 12U CubeSat mock-up on the testing platform

4.1.3 Results of fine mass balancing

The fine mass balancing performance has been assessed for all the sizes of supported CubeSats as part of the facility validation process. The procedure described in Section 3.4.2 has been followed and it is here briefly summarised. At the beginning of the fine balancing test, the vertical stepper motor carrier position is at its bottommost value. An in-plane balancing step is performed. The inertia matrix and the vertical CM to CR offset are estimated during system identification. The estimated vertical offset is compensated by moving the vertical stepper motor carrier. In-plane balancing and vertical offset compensation are repeated until the platform does not exhibit a clear stable or unstable pendulum-like motion. The disturbance torque is then estimated.

The fine balancing tests have been carried out right after coarse balancing of each CubeSat. Hence, the test cases are the same as those presented in the previous section. The fine balancing tests are here reported in detail, while Section 4.2 summarises the results of mass balancing and compares it with requirements and other facility performance levels.

4.1.3.1 1U CubeSat

Table 4.3 resumes the main steps and results of the fine mass balancing. Figure 4.14 shows one of the in-plane balancing steps performed during the procedure. Figure 4.15 shows the inertia matrix estimation results. Figure 4.16 shows one of the disturbance torque estimation results.

Table 4.3: Fine mass balancing steps and results, 1U CubeSat

Step	Description	Parameters	Results
1	In-plane balancing	Control gain used: $k_d = 0.005$ $k_p = 0.001$ $k_i = 0.0001$	Steady state RMS of gravity vector x and y components (normalised unit): $g_{x,rms} = 0.0020$ $g_{y,rms} = 0.0010$

2	Inertia and offset estimation	SG Filter parameters: • order = 3 • frame length = 41	Estimated inertia matrix [kgm^2]: $\begin{bmatrix} 0.070 & 0.000 & 0.008 \\ 0.000 & 0.072 & 0.010 \\ 0.008 & 0.010 & 0.020 \end{bmatrix}$ Estimated offset: • $r_{CM} = 5.25 \times 10^{-5} \text{ m}$ Stepper motor carrier movement needed to compensate the offset: • $\Delta r_{CM} = 0.025 \text{ m}$ $= 15957 \text{ steps}$
3	Offset compensation		Vertical mass moved of 15957 <i>steps</i>
4	Iteration of in-plane balancing and offset estimation		Vertical mass further moved up of 14286 <i>steps</i>
5	Estimation of disturbance torque	SG Filter parameters: • order = 3 • frame length = 551	Estimated disturbance torque (peak value and RMS, average over different tests): • $\tau_{D,max} = 3.69 \times 10^{-5} \text{ N m}$ • $\tau_{D,rms} = 2.43 \times 10^{-5} \text{ N m}$

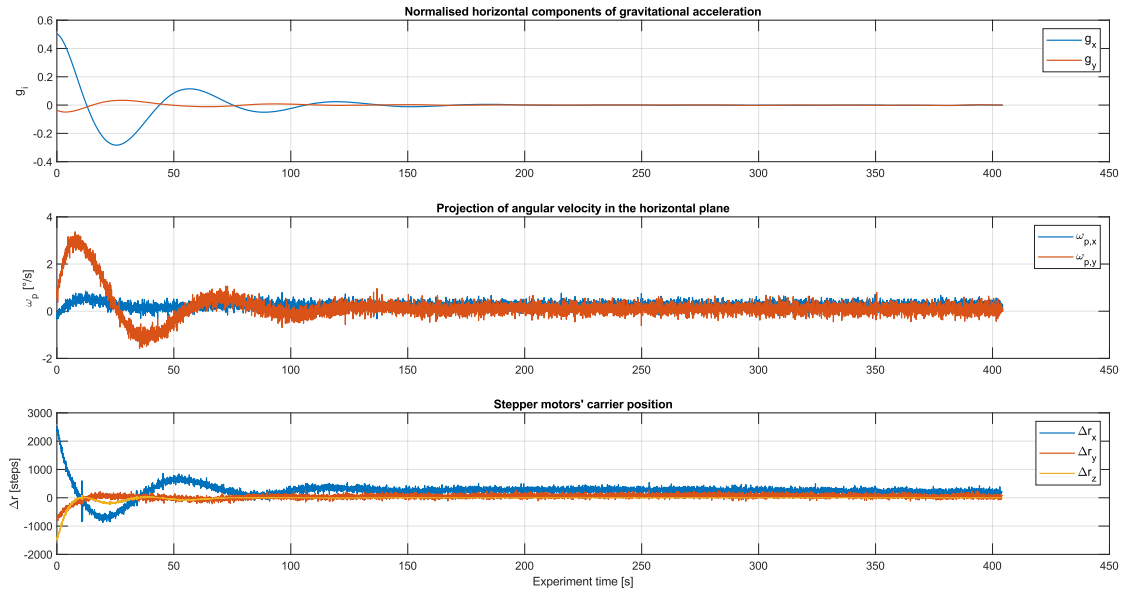


Figure 4.14: In-plane balancing step, 1U CubeSat

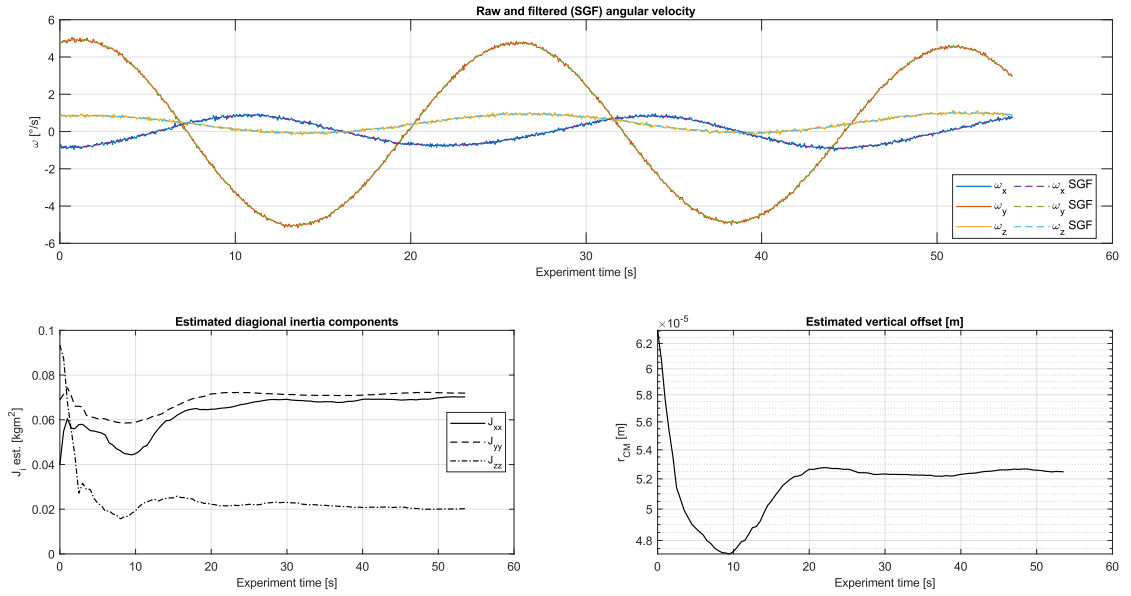


Figure 4.15: Inertia and vertical offset estimation step, 1U CubeSat

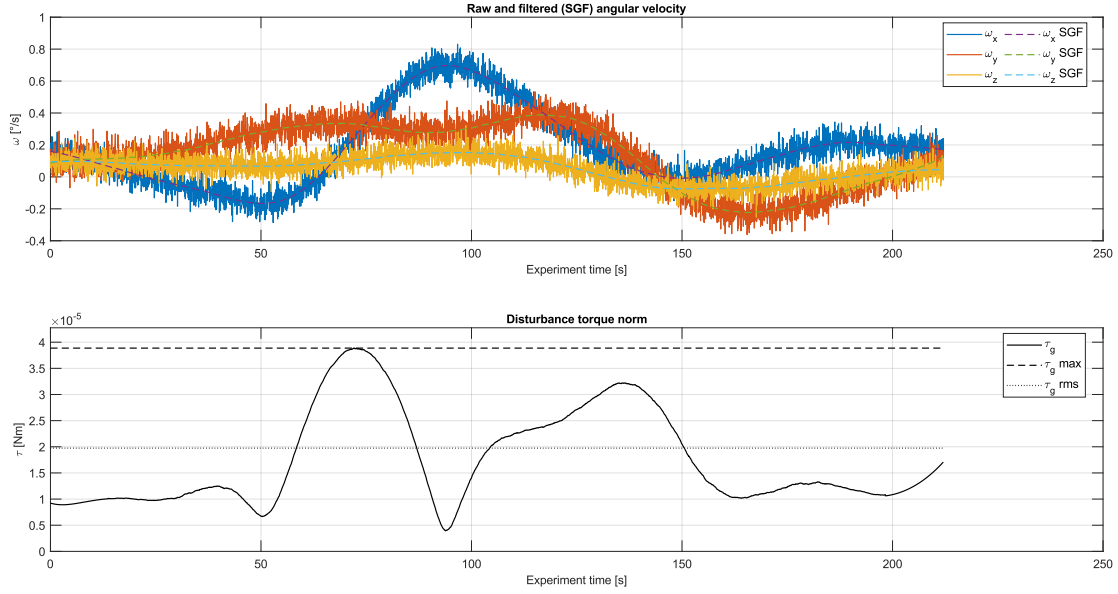


Figure 4.16: Disturbance torque estimation step, 1U CubeSat

4.1.3.2 3U CubeSat

Table 4.4 resumes the main steps and results of the fine mass balancing. Figure 4.17 shows one of the in-plane balancing steps performed during the procedure. Figure 4.18 shows the inertia matrix estimation results. Figure 4.19 shows one of the disturbance torque estimation results.

Table 4.4: Fine mass balancing steps and results, 3U CubeSat

Step	Description	Parameters	Results
1	In-plane balancing	Control gain used: • $k_d = 0.05$ • $k_p = 0.03$ • $k_i = 0.001$	Steady state RMS of gravity vector x and y components (normalised unit): • $g_{x,rms} = 0.0003$ • $g_{y,rms} = 0.0001$

2	Inertia and offset estimation	SG Filter parameters: • order = 3 • frame length = 41	Estimated inertia matrix $[kgm^2]$: $\begin{bmatrix} 0.964 & 0.052 & 0.000 \\ 0.052 & 1.022 & 0.000 \\ 0.000 & 0.000 & 0.899 \end{bmatrix}$ Estimated offset: • $r_{CM} = 3.522 \times 10^{-4} \text{ m}$ Stepper motor carrier needed to compensate the offset: • $\Delta r_{CM} = 0.128 \text{ m}$ $= 41859 \text{ steps}$
3	Offset compensation		Vertical mass moved up of 41859 steps
4	Iteration of in-plane balancing and offset estimation		Vertical mass further moved up of 14395 steps
5	Estimation of disturbance torque	SG Filter parameters: • order = 3 • frame length = 551	Estimated disturbance torque (peak value and RMS, average over different tests): • $\tau_{D,MAX} = 1.070 \times 10^{-4} \text{ N m}$ • $\tau_{D,RMS} = 6.38 \times 10^{-5} \text{ N m}$

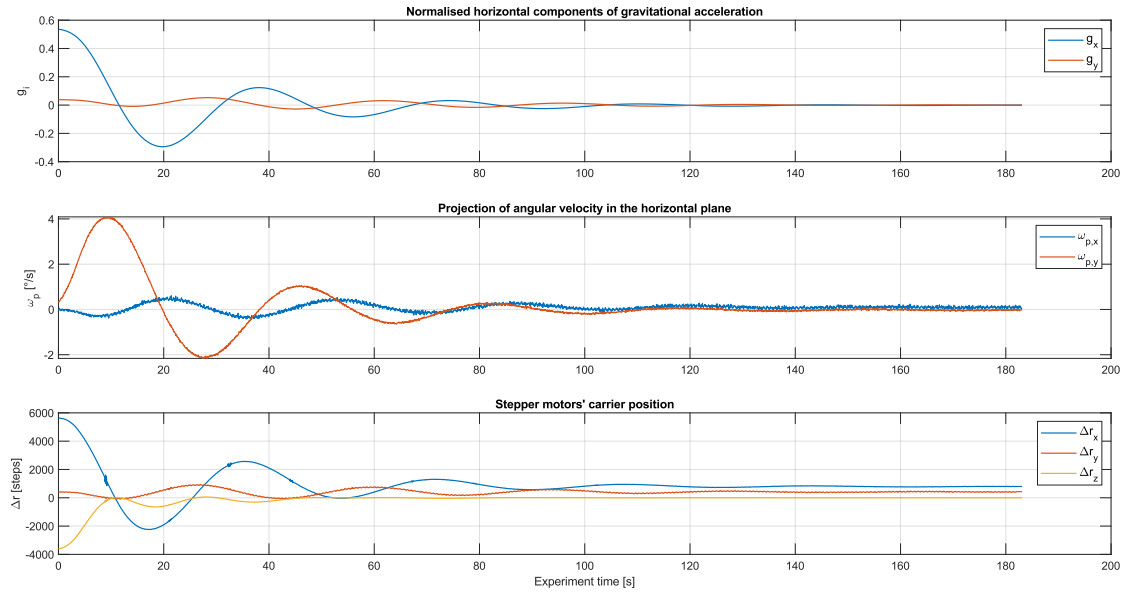


Figure 4.17: In-plane balancing step, 3U CubeSat

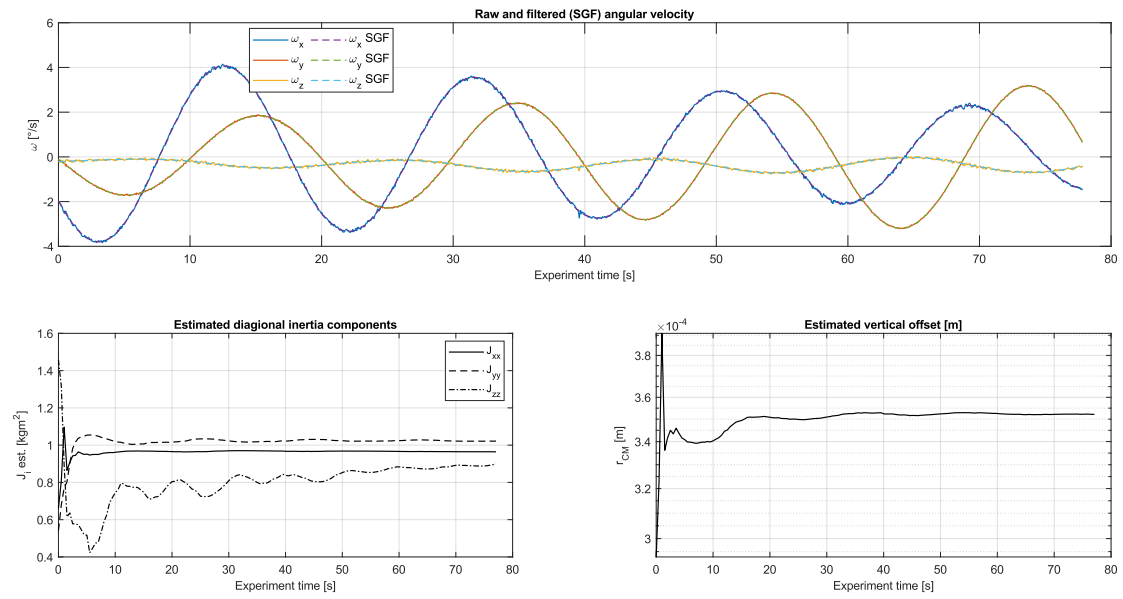


Figure 4.18: Inertia and vertical offset estimation step, 3U CubeSat

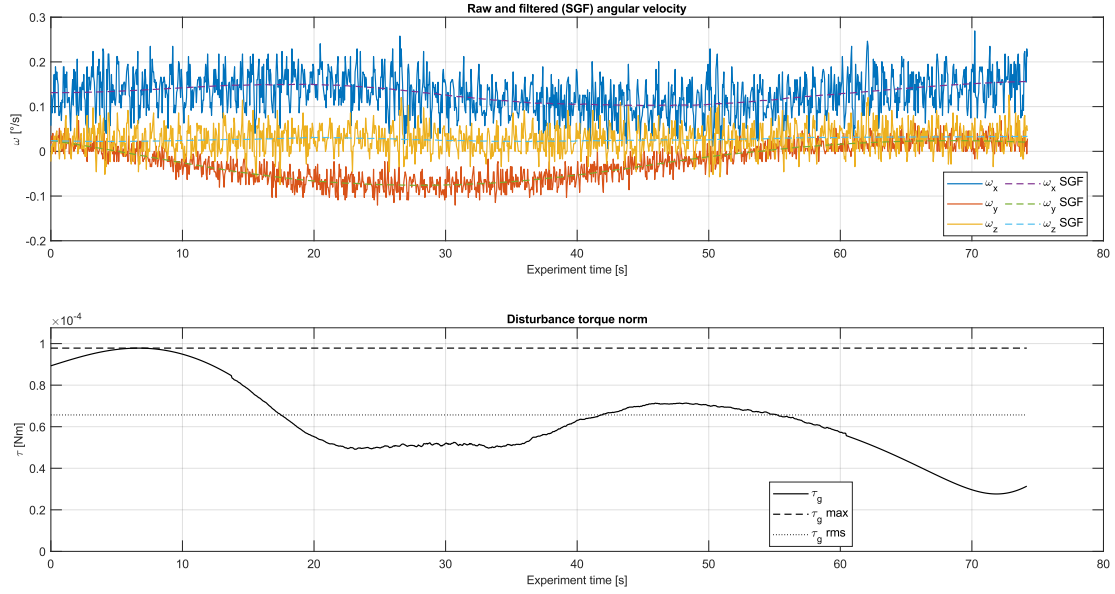


Figure 4.19: Disturbance torque estimation step, 3U CubeSat

4.1.3.3 6U CubeSat

Table 4.5 resumes the main steps and results of the fine mass balancing. Figure 4.20 shows one of the in-plane balancing steps performed during the procedure. Figure 4.21 shows the inertia matrix estimation results. Figure 4.22 shows one of the disturbance torque estimation results.

Table 4.5: Fine mass balancing steps and results, 6U CubeSat

Step	Description	Parameters	Results
1	In-plane balancing	Control gain used: $k_d = 0.06$ $k_p = 0.02$ $k_i = 0.002$	Steady state RMS of gravity vector x and y components (normalised unit): $g_{x,rms} = 0.0004$ $g_{y,rms} = 0.0004$

2	Inertia and offset estimation	SG Filter parameters: • order = 3 • frame length = 111	Estimated inertia matrix [kgm^2]: $\begin{bmatrix} 1.400 & 0.000 & 0.008 \\ 0.000 & 1.542 & 0.000 \\ 0.008 & 0.000 & 0.965 \end{bmatrix}$ Estimated offset: • $r_{CM} = 1.850 \times 10^{-4} \text{ m}$ Stepper motor carrier needed to compensate the offset: • $\Delta r_{CM} = 0.096 \text{ m}$ = 31619 steps
3	Offset compensation		Vertical mass moved up of 31619 steps
4	Iteration of in-plane balancing and offset estimation		Vertical mass further moved up of 14395 steps
5	Estimation of disturbance torque	SG Filter parameters: • order = 3 • frame length = 551	Estimated disturbance torque (peak value and RMS, average over different tests): • $\tau_{D,MAX} = 1.98 \times 10^{-4} \text{ N m}$ • $\tau_{D,RMS} = 7.73 \times 10^{-5} \text{ N m}$

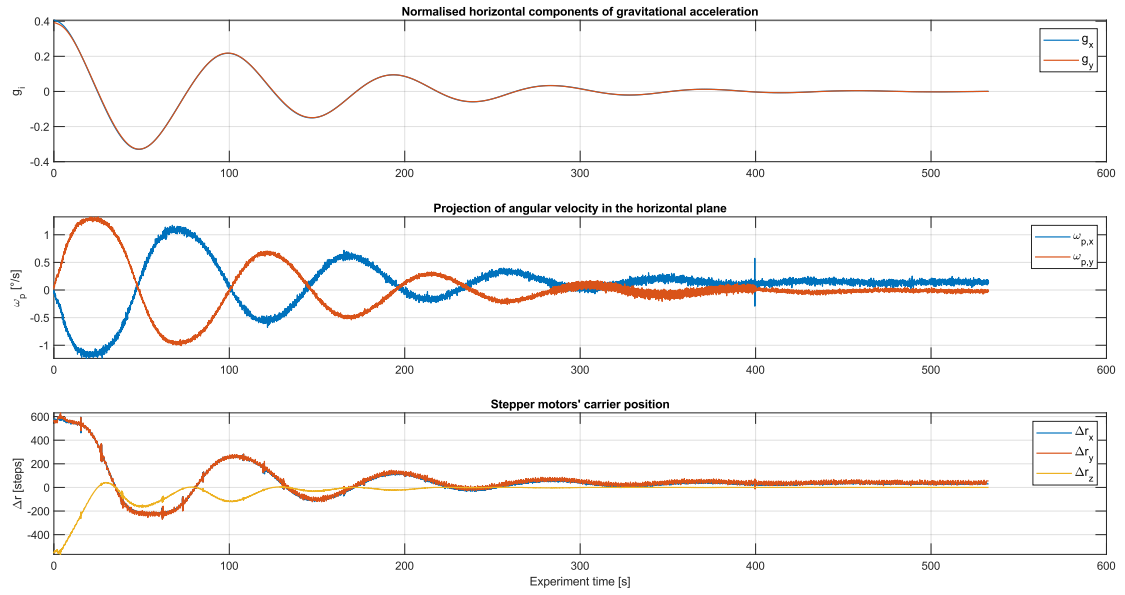


Figure 4.20: In-plane balancing step, 6U CubeSat

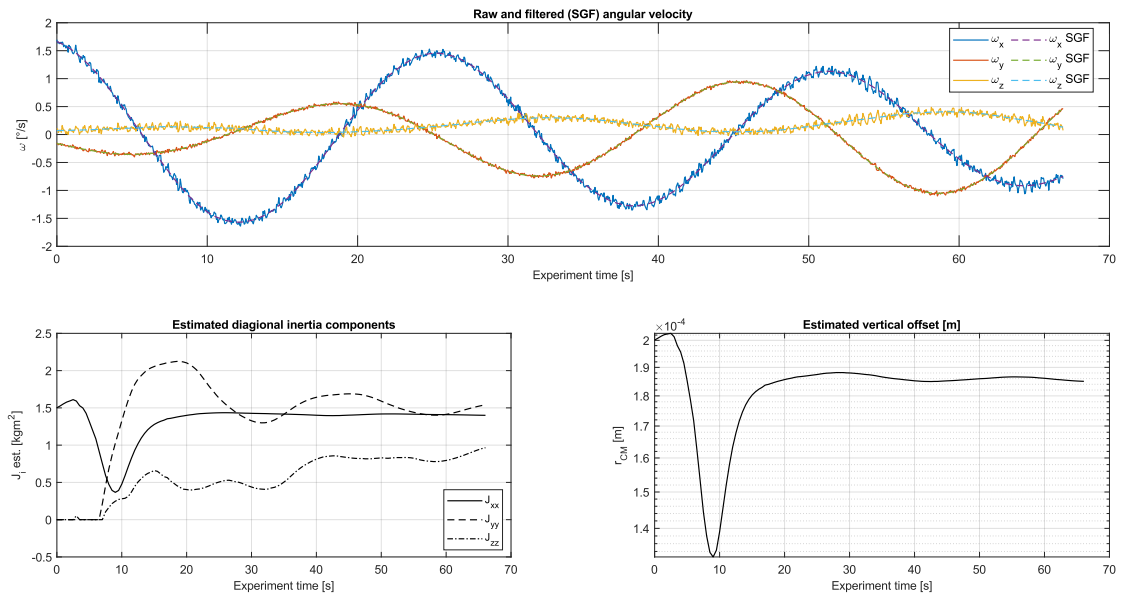


Figure 4.21: Inertia and vertical offset estimation step, 6U CubeSat

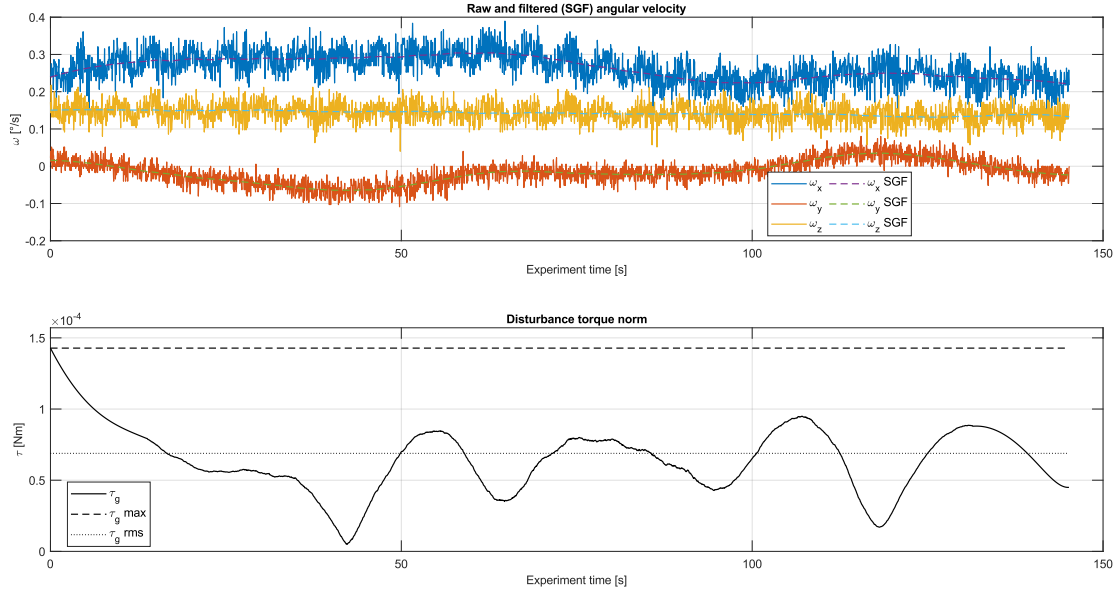


Figure 4.22: Disturbance torque estimation step, 6U CubeSat

4.1.3.4 12U CubeSat

Table 4.6 resumes the main steps and results of the fine mass balancing. Figure 4.23 shows one of the in-plane balancing steps performed during the procedure. Figure 4.24 shows the inertia matrix estimation results. Figure 4.25 shows one of the disturbance torque estimation results.

Table 4.6: Fine mass balancing steps and results, 12U CubeSat

Step	Description	Parameters	Results
1	In-plane balancing	Control gain used: • $k_d = 0.06$ • $k_p = 0.02$ • $k_i = 0.002$	Steady state RMS of gravity vector x and y components (normalised unit): • $g_{x,rms} = 0.002$ • $g_{y,rms} = 0.003$

2	Inertia and offset estimation	SG Filter parameters: • order = 3 • frame length = 41	Estimated inertia matrix $[kgm^2]$: $\begin{bmatrix} 2.306 & 0.321 & 0.000 \\ 0.321 & 2.627 & 0.000 \\ 0.000 & 0.000 & 1.865 \end{bmatrix}$ Estimated offset: • $r_{CM} = 1.913 \times 10^{-4} \text{ m}$ Stepper motor carrier needed to compensate the offset: • $\Delta r_{CM} = 0.156 \text{ m}$ $= 51073 \text{ steps}$
3	Offset compensation		Vertical mass moved up of <i>51073 steps</i>
4	Iteration of in-plane balancing and offset estimation		Vertical mass further moved up of <i>7208 steps</i>
5	Estimation of disturbance torque	SG Filter parameters: • order = 3 • frame length = 551	Estimated disturbance torque (peak value and RMS, average over different tests): • $\tau_{D,MAX} = 3.46 \times 10^{-4} \text{ N m}$ • $\tau_{D,RMS} = 2.04 \times 10^{-4} \text{ N m}$

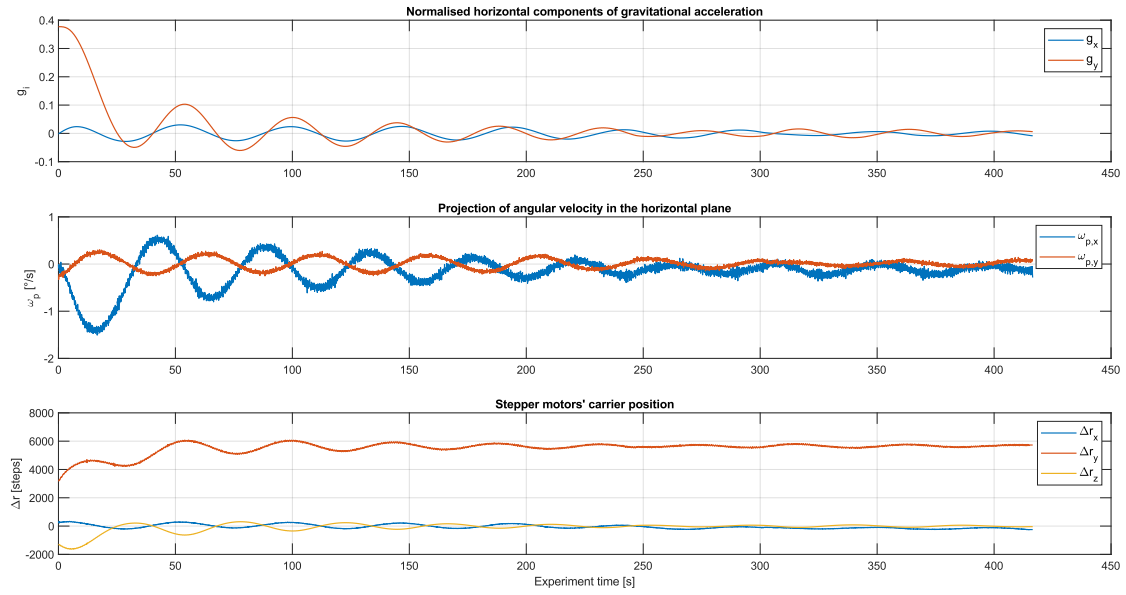


Figure 4.23: In-plane balancing step, 12U CubeSat

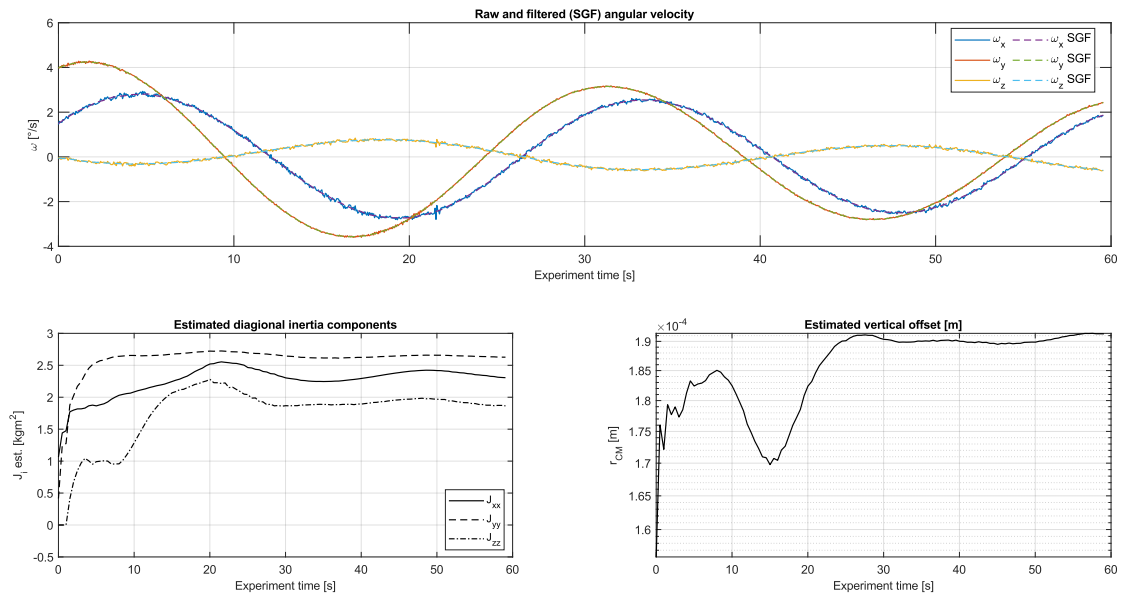


Figure 4.24: Inertia and vertical offset estimation step, 12U CubeSat

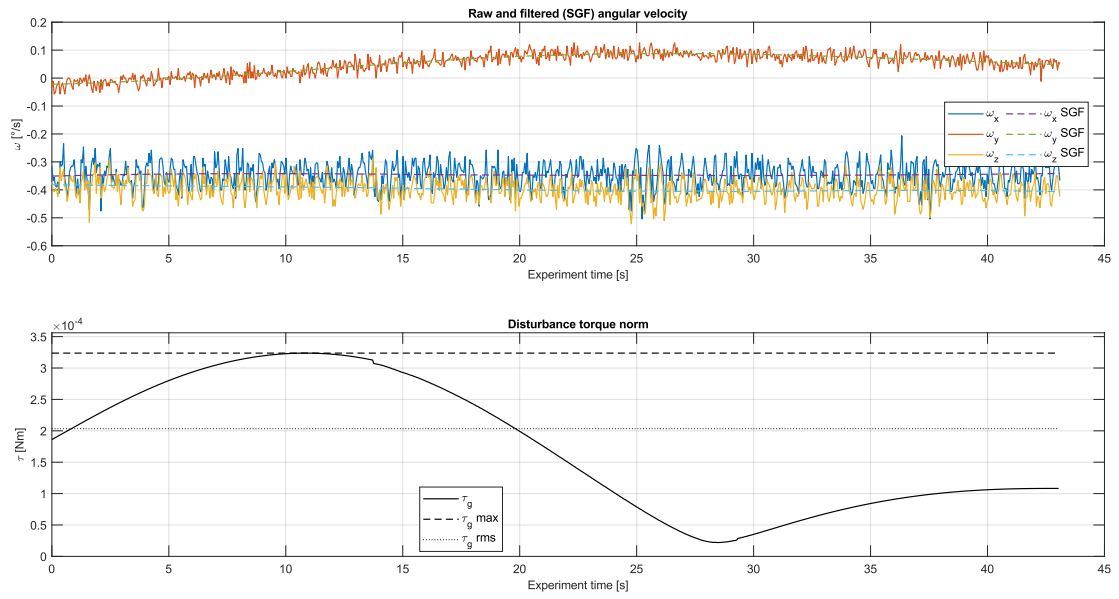


Figure 4.25: Disturbance torque estimation step, 12U CubeSat

4.2 Summary of mass balancing results

Chapter 1 highlighted a gap in existing CubeSat ADCS testing facilities in terms of CubeSat hosting capabilities, simulation capabilities and residual torque level. In particular, the need for a facility capable of hosting CubeSats FM or EM with size from 1U to 12U, with various sensor stimulus and with low level of disturbance torque arises. To systematically and reliably provide this environment, the design and verification of the mass balancing tools and procedures is a critical step. The results presented in this chapter showed that the CubeDynA mass balancing system can provide low level of gravity torque for all the target classes of CubeSats.

In Table 4.7, the obtained results are compared with the calculated theoretical level of disturbance torques acting in space and with the requirements. The level of residual disturbance torque is slightly outside requirements for the 1U platform of 22% and on the 3U-12U platform of 142 % on average. The RMS level of estimated disturbance torque in particular resulted to be higher for the 12U CubeSat, while for the 3U and 6U CubeSats the values are comparable. This result can be attributed to the relative short test campaign with the 12U CubeSat, compared to the one with the other CubeSats. It is likely that a prolonged test campaign with the 12U CubeSat would have resulted in a level of disturbance torques comparable with the one measured with the 3U and 6U CubeSat.

The level of disturbance torque however is in line with the state of the art and it does not hinder the possibility of obtaining many of the facility utilisation cases presented in Section 3.1. Furthermore, the control authority of some actuators can be increased to regain simulation fidelity, by, for instance, increasing the magnetic field strength with respect to the one present in orbit. Figure 4.26 shows how the CubeDynA facility compares to the other CubeSat ADCS testing facilities in terms of residual torque and simulation capabilities at varying CubeSat size.

A few limitations of the CubeDynA facility simulation fidelity should be highlighted. For any air bearing-based facility, the disturbance torque does not strictly relate to the environmental in-orbit torque. The gravity torque indeed, acts constantly in the horizontal plane, eventually leading to actuators saturations and hence limiting the test duration. This can be also affected by the platform rota-

tional range, limited in CubeDynA to $\pm 30^\circ$ for the roll and pitch axis. The test duration can be increased by appropriately mapping the yaw free axis to an inertial axis in simulation. The fidelity is also affected by the additional parasitic mass of the balancing system, which changes the effective DUT moment of inertia. Overall, these limitations do not constitute an obstacle to CubeSat ADCS testing.

Finally, the CubeDynA facility can be adapted via minor modifications to tests bigger CubeSats or satellites of the nanosatellite or microsatellite class which do not strictly belong to the CubeSat standard, as long as their size and mass footprint are within the envelope of the already compatible DUT.

Table 4.7: Comparison of in-orbit torque with CubeDynA facility torque

Disturbance Torque	1U	3U	6U	12U
In-orbit torque [Nm]	1.22×10^{-6}	4.69×10^{-6}	1.21×10^{-5}	1.82×10^{-5}
Requirement torque rms [Nm]	2×10^{-5}	5×10^{-5}	5×10^{-5}	5×10^{-5}
CubeDynA torque rms [Nm]	2.43×10^{-5}	6.38×10^{-5}	7.73×10^{-5}	2.04×10^{-4}

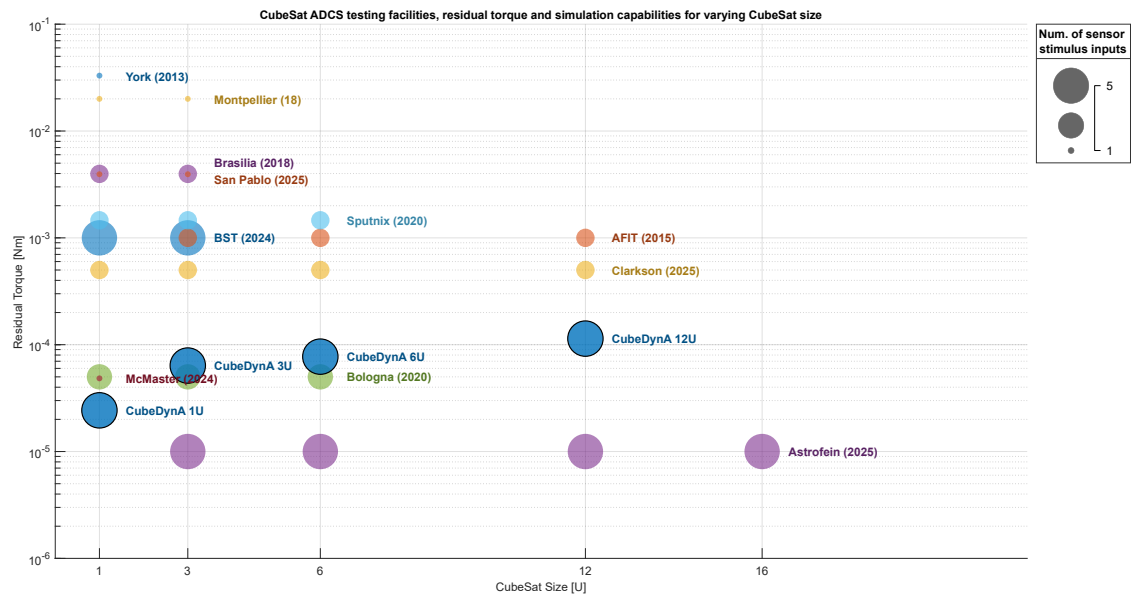


Figure 4.26: Attitude testing facility (including CubeDynA) residual torque level at varying CubeSat size. The bubble size represents the number of sensor stimulus, with 1 being only the air bearing.

4.3 Communication module test results

The communication module is used in the facility to ensure wireless data transmission between the facility ground station and the DUT (one way) and between the user ground station and the DUT (two ways). The communication module is tested in its ability to reliably forward packets and introduce minimum delay. During nominal operation, the DUT is kept synchronised with the facility computer through the synchronisation function. The function sends to the DUT (through the communication module) a synchronisation packet at the start of simulation and then, optionally, at specified regular intervals (e.g. every 10 seconds). The synchronisation packet is based on the CCSDS day segmented time code (CDS). It contains, apart from a source, flag, and speed factor field, the ground station time and the scenario simulated time. The DUT is responsible for updating its clock based on the synchronisation packet.

The communication module testing is divided into three parts: the first part regards the evaluation of the difference in between the facility computer and DUT absolute clock time. This is needed to be able to perform the second tests, consisting in the evaluation of the delay between the generation of a packet on the facility computer and its delivery to the DUT. Finally, the quality of the transmission between the DUT and an emulated user ground station is evaluated. Figure 4.27 represents the test setup, common for the three experiments and here briefly described. A single computer is connected to the LAN for the exchange of packets. A USB to UART converter is used to change the state of a pin for the synchronisation test. In the synchronisation test, an oscilloscope is connected to both the USB to UART converter and to the DUT. The DUT is simulated by using another Raspberry Pi Zero board. Another computer is used for debugging and connected via SSH to the communication module and to the DUT for debugging purposes. The same computer is used to simulate an user ground station that needs to communicate with the DUT. The results of the three tests are presented in Section 4.3.1, Section 4.3.2 and Section 4.3.3. Overall, the communication module is able to keep the synchronisation delay below 0.1 s and to reliably forward all the packets when using the CAN interface. When using the I2C interface instead the delay is slightly above 0.1 s and a portion of the packet is missing. This last fact depends solely on the specific device used to emulate

the DUT, which does not natively support the I2C communication as slave and for which a custom library was implemented. The issue is expected to not be present on DUT natively supporting I2C communication as I2C slave.

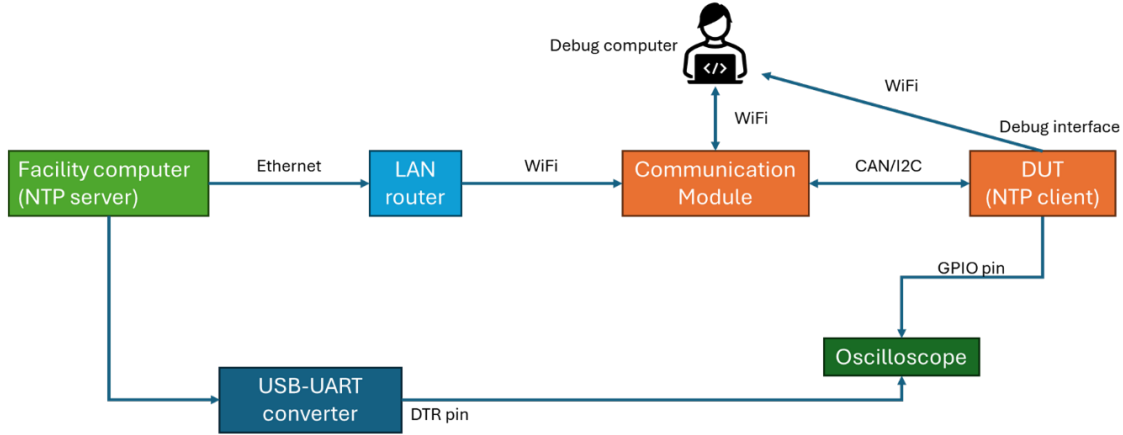


Figure 4.27: Communication module test setup schematic

4.3.1 Absolute clock synchronisation test

Before running the path delay estimation test, the absolute clock reference should be known for the facility computer and DUT. The two devices are synchronised using the Network Time Protocol (NTP). The facility computer is set to run a NTP server while the DUT is the NTP client. The synchronisation is measured with a test signal generated by both the GS (reference) and the DUT. The two test signals are simple square waves toggling at integer tenth second of a minute. If the two devices are synced, then the signals shall toggle at the same time. The two clock signals are collected into a double-input oscilloscope in trigger mode. By measuring the time offset between the rising edges, the synchronisation error is measured. Figure 4.28 shows an example of the two rising edges shown in the oscilloscope. Figure 4.29 and Figure 4.30 show the distribution and mean value of the delay for the CAN and I2C interfaces, on average the synchronisation delay is equal to 12 *ms*.

4.3.2 Path delay estimation

Nominally, the DUT sets its clock based on the synchronisation packet received from the facility computer. In the test setup, however, the DUT does not use

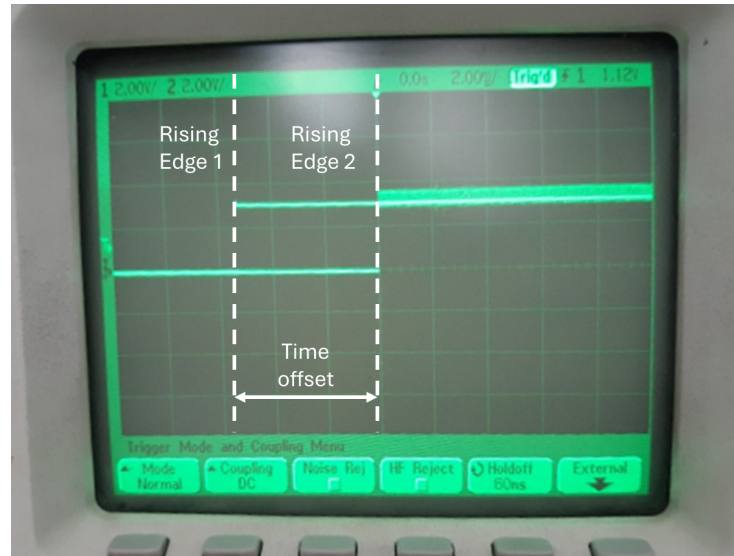


Figure 4.28: Synchronisation test, comparison of signals rising edges on oscilloscope

directly the packet to set its clock, rather a path delay between the facility computer and DUT is measured. This is done to make the test independent of the DUT performance in setting its clock, which is machine dependent. The time at which the packets are received on the DUT is compared with the time at which they are generated. Figure 4.31 and Figure 4.32 show the distribution and the mean value of the path delay for the CAN and I2C interfaces added to the 12 *ms* synchronisation delay.

A few packets are delivered with a large delay, the value at 1σ considered to exclude these outliers. The updated graphs are shown in Figure 4.33 and Figure 4.34. For the CAN interface the mean value is below 100 *ms*, while for the I2C interface it is slightly above of it. For both interfaces there is a portion of the delay which is to be attributed to the transit time through the WiFi router. This was assessed by comparing the sending timestamp leaving the workstation with a network protocol analyser and the arrival time stamp at the communication module (synchronised with NTP). The remaining part of the delay is given by the communication interface between communication module and DUT. For the I2C interface the increase in path delay with respect to the CAN is caused by time delay of about 40 *ms* artificially introduced to avoid overflow of the FIFO buffer.

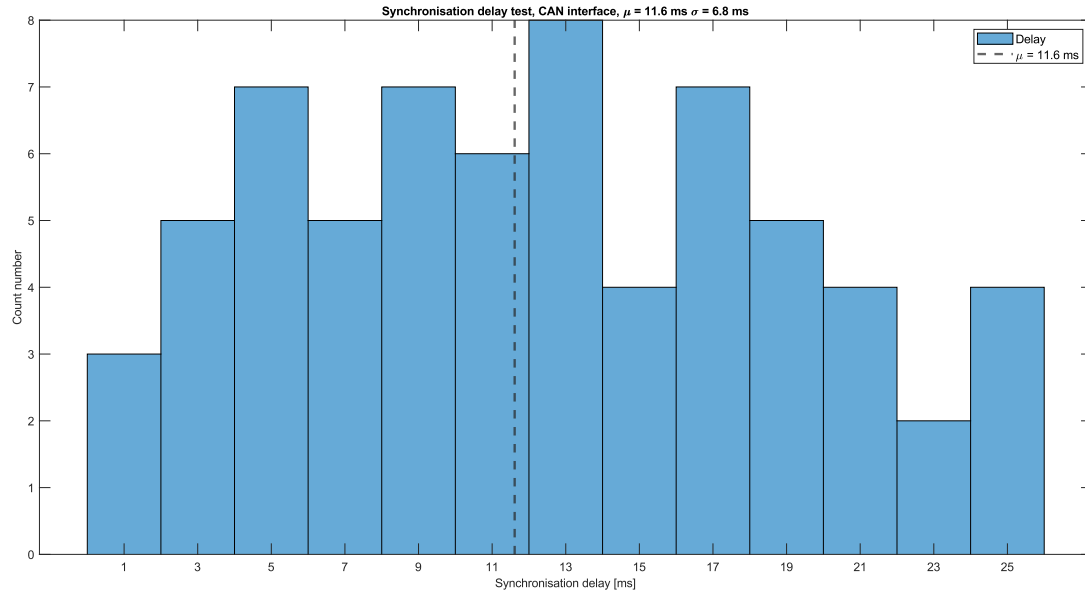


Figure 4.29: Synchronisation test, delay with CAN interface

4.3.3 Quality of transmission evaluation

The correct transmission of the packets from the DUT to a user ground station and from the ground station to the DUT is checked by printing the packet content on a terminal interface. For what concerns the CAN interface, all packets are received uncorrupted on both ways. When using the I2C interface instead, the communication reliably works only in one direction. The packets sent from the GS are correctly received by the Raspberry Pi emulating the DUT. The packets generated by the Raspberry instead and received by the GS result in being corrupted and some are missing (80% of packets are received). This is a result of the fact that the Raspberry Pi does not natively support the I2C communication in slave mode and that a third-party library has been used for the scope.

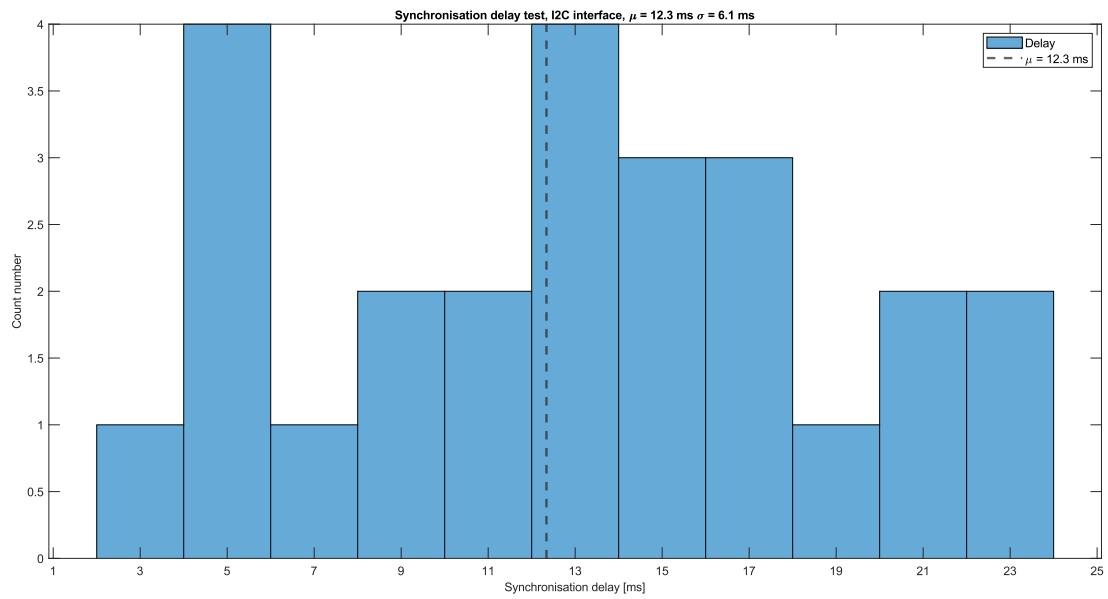


Figure 4.30: Synchronisation test, delay with I2C interface

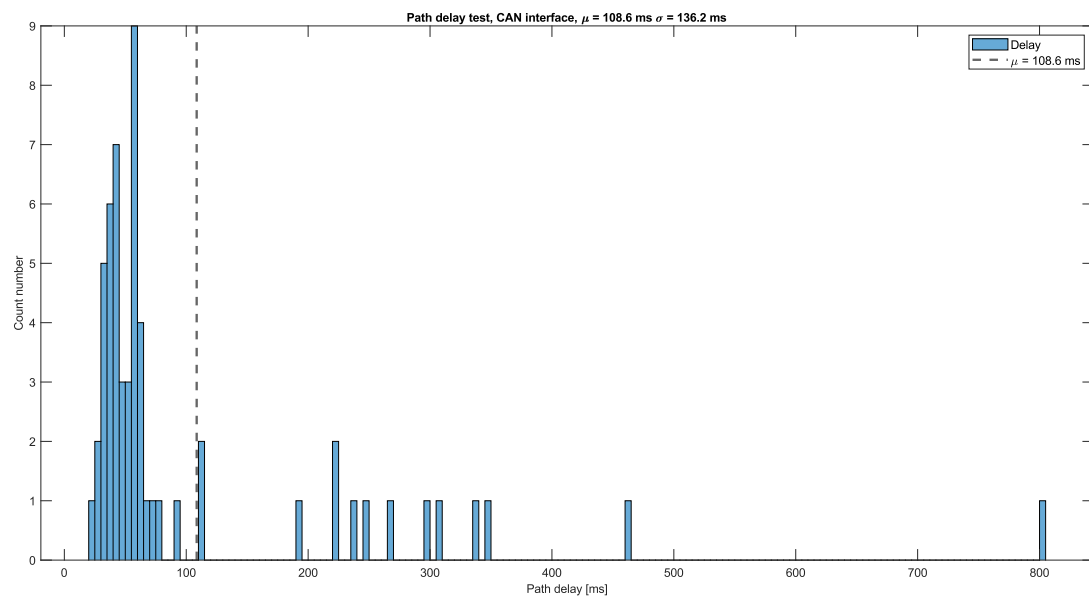


Figure 4.31: Path delay test, delay with CAN interface

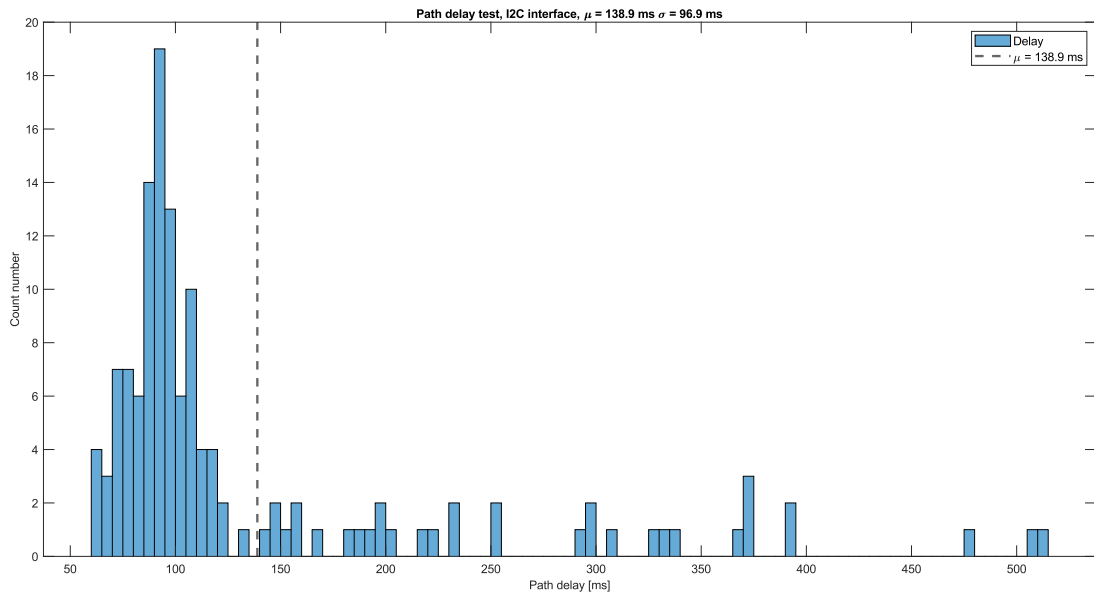


Figure 4.32: Path delay test, delay with I2C interface

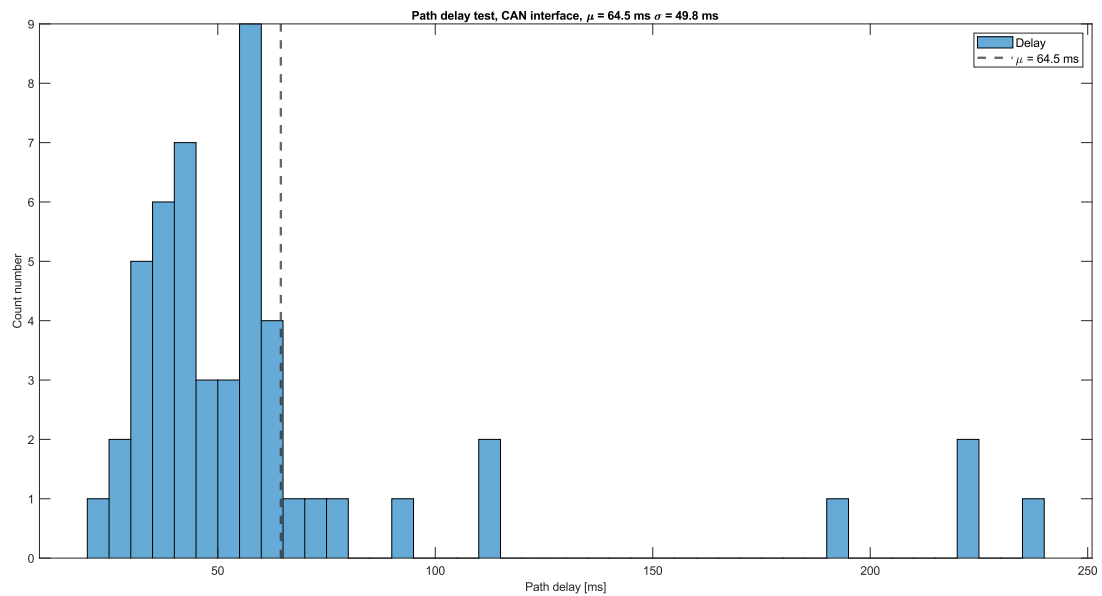


Figure 4.33: Path delay test, delay with CAN interface, value at 1σ

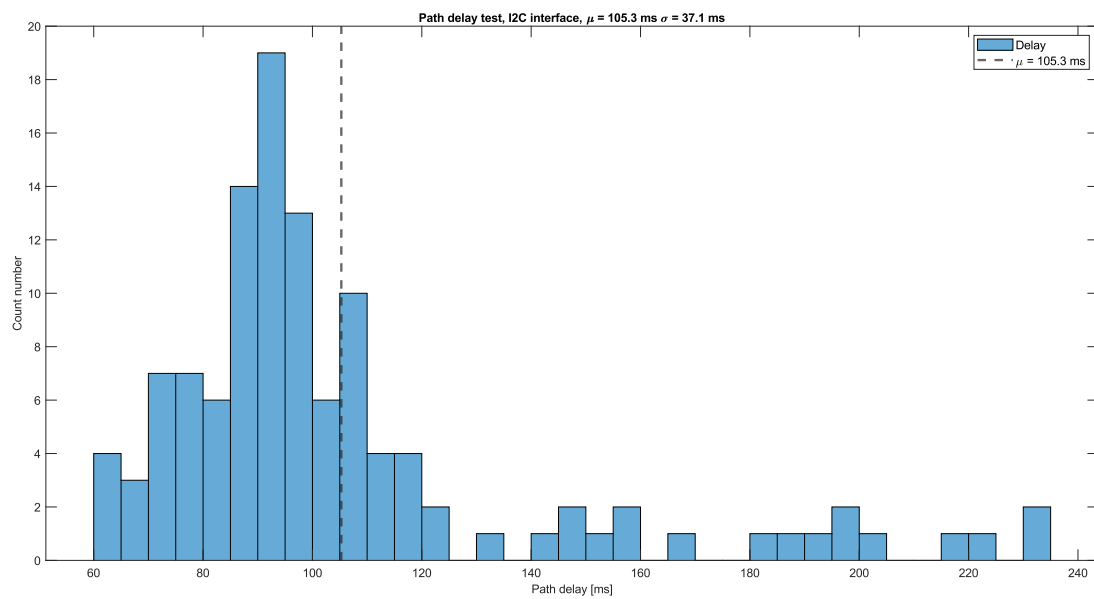


Figure 4.34: Path delay test, delay with I2C interface, value at 1σ

5

Part I, Conclusions

The first part of this PhD work focused on the development of the mass balancing system and of the communication module of the CubeDynA facility. The mass balancing system represents a fundamental component of the facility, enabling to reproduce a low torque environment, a key aspect of the rotational dynamics of an object in space. In the balancing process, the position of the rotating platform centre of mass is brought at sub-micron distance from the centre of rotation. The communication module enables wireless communication between the ground station and the device under test and can be used to forward simulated star tracker measurement based on the satellite real attitude.

The CubeDynA facility bridges a gap in existing CubeSat ADCS testing facilities as it allows testing flight models of CubeSat with sizes from 1U to 12U, with physical stimulus of the most common sensors and actuators, in a low torque environment.

Since the facility is designed to accommodate various CubeSat sizes and configurations, the balancing system must provide a large control envelope for the centre of mass position while maintaining high positional accuracy. These two requirements are intrinsically conflicting. To address them, the balancing system has been divided into two parts: a coarse balancing part dedicated to compensating for the macroscopic distance between the centre of mass and centre of rota-

tion, and a fine automatic balancing part, which compensates for the remaining microscopic offset.

The design and implementation of both subsystems constituted a major part of the candidate's research activity. In particular, the fine balancing subsystem needs to be automatic in order to achieve the specified accuracy level. The system integrates an in-plane balancing stage, which dynamically shifts the centre of mass position based on the platform attitude, and a system identification part to compensate for the residual offset. The level of residual torque after the balancing process is estimated. Experimental results showed performances slightly outside the requirements, yet consistent with the current state of the art and among the lowest disturbance levels reported in the literature for comparable facilities.

When it comes to simulation fidelity for ADCS performance testing, the facility is affected by a few limitations, related to residual torque level and time variation, which does not directly relate to the one present in-orbit. The level of the residual torque and the air bearing rotational range could limit the test duration. A few mitigation strategy however exist, consisting in increasing the strength of the magnetic field for enhanced magnetic actuators authority or appropriately map the simulation inertial axis to the air bearing free axis. Functional and polarity testing are instead not directly affected by these limitations.

A further contribution of the candidate concerns the design of the communication system between the device under test and the ground station. The disturbance minimisation objective naturally leads to the adoption of a wireless communication system. Two communication protocols commonly used onboard CubeSats were implemented, and the system was experimentally validated in terms of latency and transmission reliability. The performance is fully within requirements when using the CAN interface. The I2C interface performance instead is slightly outside requirements, but its evaluation has been influenced by the device used to simulate the CubeSat during testing and should be evaluated with a different device.

As a final contribution, the facility was commissioned and installed at the ESTEC, where training of personnel was performed.

Overall, the CubeDynA facility represents a unique experimental platform, capable of testing CubeSat flight models of different sizes and configurations under realistic sensor and actuator stimulation, within an environment characterised

by very low disturbance torques. Such a facility enables a truly “test-like-you-fly” Verification & Validation philosophy, eventually contributing to increasing the success rate of CubeSat missions.

Part II:
The High-Performance
Stabilisation Breadboard
at ESA/ESTEC

6

Part II, Introduction

Modern space missions are characterised by stringent pointing requirements, requiring sub-arcsec or better accuracy (Bhatia, 2021). A few examples of these missions include the James Webb Space Telescope (JWST) (Chayer et al., 2010), the Euclid mission (Mellier et al., 2024) and the GAIA mission (Prusti et al., 2016). Also, future small satellite missions such as CubeSpec (Peri et al., 2025) and CLICK (Cahoy et al., 2019) will be characterised by extremely fine pointing. On small satellites, performance is intrinsically limited (Janschek et al., 2007), (Douglas et al., 2021). To overcome the limitation of the attitude determination and control system, often a dual-stage pointing mechanism is employed. The fine pointing task is divided into two stages: the ADCS controls the orientation of the whole spacecraft structure, another pointing system acts directly on the payload. For Line Of Sight (LOS) control Fast Steering Mirror (FSM) are often used (Chayer et al., 2010), (Bronowicki, 2006), while other systems use piezoelectric actuation (Smith et al., 2018) or mass-spring-damper (Philip Linden, PlanetFollow et al., 2021). These LOS control active system bandwidth is limited to a few Hz , but microvibration can act on a wider frequency range. For instance the JWST FSM attenuates tip and tilt vibrations below 0.4 Hz (Bronowicki, 2006). The term **microvibration** refers to mechanical vibration of low amplitude with frequency from 1 Hz to 1 kHz (Aglietti et al., 2004). A series of components on

board a spacecraft, such as solar arrays, drive mechanisms, cryo-coolers and reaction wheels, can generate microvibrations. The current baseline for microvibrations mitigation consists of using pure passive isolators (Preda et al., 2018). These isolators, however, act as a low-pass filter (Le, 2017) and low frequency disturbance can propagate through. Along with passive methods, several active isolation methods have been developed. They consist of using external actuators and sensors to provide control force/torque to suppress microvibration in some frequency range (Liu et al., 2015). Hybrid isolation techniques, which combine an active platform with a set of passive isolators, have also been developed. Typically, the actuators are arranged in a hexapod configuration (Stewart platform) to provide both force and torques. This type of technique combines the advantages of passive and active isolation to mitigate microvibration in a broad frequency range. The ESA GNC, AOCS & Pointing Laboratory has developed a high-performance stabilisation breadboard to validate and assess the pointing stability performances of future pointing systems in the presence of microvibrations. Depending on the source of disturbances, pointing stability is improved by a factor of 10 or more.

This system is complementary to line-of-sight stabilisation systems in terms of disturbance type mitigation, accommodation, and principle: indeed, this system mitigates wheels and cryo-coolers microvibrations instead of solar array drive mechanisms disturbances, and it is set below a payload or below the source of disturbances instead of being set inside the payload. Besides, it uses accelerometers and/or gyroscopes and shakers instead of an image sensor and fast steering mirrors. It is also different from a hexapod, which uses piezo sensors and actuators. This technology enables meeting challenging pointing stability performances of future Earth Observation of Science missions without cold gas systems, even in the presence of cryo-coolers.

Different types of controllers 'algorithms have been designed (LTI, LPV, SISO, MIMO), tuned and tested by simulation and some of them have been validated on a given configuration of the breadboard, including system identification. The already tuned controllers are not adapted to any new breadboard configuration: a new breadboard configuration needs system identification and controllers re-tuning. The proposed research aims at completing the work already done by automating the full process, including system identification on a new test setup, controllers tuning, and validation, followed by a complete validation test cam-

paign comparing the various controllers' performances. This will increase the technology operational Technology Readiness Level (TRL).

Part of the work on the ESA high-performance stabilisation breadboard was carried out at ESA-ESTEC facilities. The visiting research period at ESA-ESTEC was supported by ESA and undertaken EISI I-2024-09634 agreement.

7

Preliminaries

This chapter introduces the robust control theory framework, which constitutes the theoretical background for Part II of the thesis.

7.1 Robust control

The fundamental concepts of robust control are here briefly presented. Most of this chapter is based on [Zhou and Doyle \(1999\)](#) and on [Innocenti \(2021\)](#) lecture notes for what concerns the definition of robust stability and robust performance. Section 7.1.1 introduces the state space description for linear system, as well as the concept of controllability and observability Gramians. Section 7.1.2 introduces the concept of Nominal Stability (NS) and Nominal Performance (NP). The interconnected system representation is presented in section 7.1.3 together with the concept of system uncertainty. The structured singular value μ and the concept of Robust Stability (RS) and Robust Performance (RP) are presented in Section 7.1.4. Finally, Section 7.1 presents the robust control synthesis techniques used in Part II of the thesis.

7.1.1 Linear systems

A finite dimensional linear time invariant dynamical system can be expressed in the state space form as:

$$\dot{\mathbf{x}} = A\mathbf{x} + B\mathbf{u}, \quad \mathbf{x}(t_0) = \mathbf{x}_0 \quad (7.1)$$

$$\mathbf{y} = C\mathbf{x} + D\mathbf{u} \quad (7.2)$$

where $\mathbf{x}(t) \in \mathbb{R}^n$ is the system state, $\dot{\mathbf{x}}(t)$ its time derivative, $\mathbf{u}(t) \in \mathbb{R}^m$ is called the system input, and $\mathbf{y}(t) \in \mathbb{R}^p$ is the system output. A, B, C, D are matrices with according dimensions. When $m = p = 1$, the system is called Single-Input Single-Output (SISO), conversely when they are different from 1, is called Multiple-Input Multiple-Output (MIMO).

The dynamic system described by Equations 7.1 and 7.2 (or the pair A, B) is said to be **controllable** if for any initial state $\mathbf{x}_0$ and final state $\mathbf{x}(t_1 > 0) = \mathbf{x}_1$ there exist a piecewise continuous input $\mathbf{u}(t)$ such that $\mathbf{x}(t_1) = \mathbf{x}_1$ is satisfied, for any $t_1 > 0$. There are different ways of verifying controllability, one of these is through the **Controllability Gramian**. Given the Lyapunov equation:

$$AP + PA^\top + BB^\top = 0 \quad (7.3)$$

with stable A , a pair (A, B) is controllable if and only if the solution to the above Lyapunov equation is positive definite, where P is the controllability Gramian. The dynamic system described by Equation (7.1) and Equation (7.2) (or the pair C, A) is said to be **observable** if the initial state $\mathbf{x}_0$ can be determined from the time history of the input $\mathbf{u}(t)$ and the output $\mathbf{y}(t)$ in the interval of $[0, t_1]$, for any t_1 . There are different ways of verifying observability, one of these is through the *Observability Gramian*. Given the Lyapunov equation:

$$A^*Q + QA + C^*C = 0 \quad (7.4)$$

with stable A , a pair (C, A) is observable if and only if the solution to the above Lyapunov equation is positive definite, where Q is the *observability Gramian*. Often, what is available are transfer matrices rather than a state space representation. Different techniques to obtain a state space representation exist. A state

space representation associated with a transfer matrix is said to be minimal if A has the smallest possible dimension. This is equivalent to verifying that the pair (A, B) is controllable and the pair (C, A) is observable.

7.1.2 Nominal stability and performance

To define the stability of a closed loop system, we introduce the concept of $\mathcal{L}_\infty$ and $\mathcal{H}_\infty$ spaces. $\mathcal{L}_\infty(j\mathbb{R})$ or simply $\mathcal{L}_\infty$ is a Banach space of matrix-valued functions that are bounded on $j\mathbb{R}$ with norm:

$$\|F\|_\infty := \operatorname{ess\,sup}_{\omega \in \mathbb{R}} \bar{\sigma}[F(j\omega)] \quad (7.5)$$

$\mathcal{H}_\infty$ is a subspace of $\mathcal{L}_\infty$ with functions that are analytic and bounded in the open right-half plane. The $\mathcal{H}_\infty$ norm is defined as:

$$\|F\|_\infty := \sup_{\operatorname{Re}(s) > 0} \bar{\sigma}[F(s)] = \sup_{\omega \in \mathbb{R}} \bar{\sigma}[F(j\omega)] \quad (7.6)$$

The real rational subspace of $\mathcal{H}_\infty$ is denoted by $\mathcal{RH}_\infty$, which consists of all proper and real rational stable transfer matrices.

Consider the standard feedback configuration of Figure 7.1. r is the reference signal, u is the input signal, d_i is the input disturbance, $u_G = u + d_i$ is the input to the plant G , y_G is the plant output, d is the output disturbance, y is the output signal, n is the sensor noise, $e = r - y_G - d - n$ is the error signal.

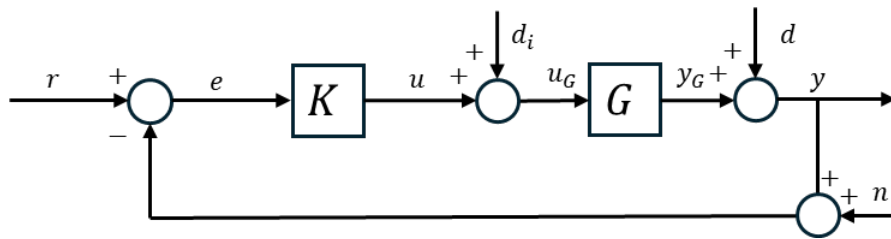


Figure 7.1: Standard feedback configuration

If we suppose that all the external signals r, n, d and d_i are specified and that the closed-loop transfer matrices from them to u are respectively well-defined and proper, we can regroup the feedback system to resemble the one in Figure 7.2. The system is well-posed if and only if the transfer matrix from the external input $[w_1 \ w_2]$ to the error e_1 exist and is proper. It can be shown that if $-K \in$

7.1. Robust control

$\mathcal{RH}_\infty$ then the system of Figure 7.2 is internally stable if and only if it is well-posed and $G(I + KG)^{-1} \in \mathcal{RH}_\infty$. When we deal with an uncertain system and $G = G_{nom}$ nominal plant, we call this condition **Nominal Stability (NS)**.

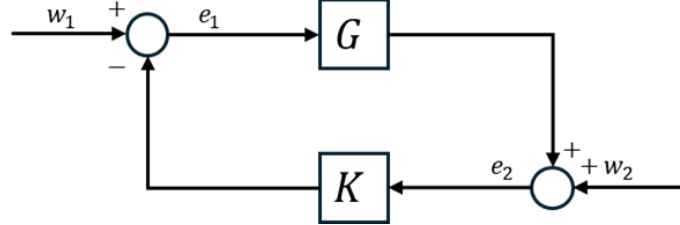


Figure 7.2: Internal stability analysis diagram

We reference again to the structure of Figure 7.1. It can be shown that if the closed-loop system satisfies internal stability, then the following equations hold:

$$\mathbf{y} = T_o(\mathbf{r} - \mathbf{n}) + S_o G \mathbf{d}_i + S_o \mathbf{d} \quad (7.7)$$

$$\mathbf{r} - \mathbf{y} = S_o(\mathbf{r} - \mathbf{d}) + T_o \mathbf{n} - S_o G \mathbf{d}_i \quad (7.8)$$

$$\mathbf{u} = K S_o(\mathbf{r} - \mathbf{n}) - K S_o \mathbf{d} - T_i \mathbf{d}_i \quad (7.9)$$

$$\mathbf{u}_p = K S_o(\mathbf{r} - \mathbf{n}) - K S_o \mathbf{d} + S_i \mathbf{d}_i \quad (7.10)$$

where:

$$S_i = (I + KG)^{-1} \quad (7.11)$$

$$S_o = (I + GK)^{-1} \quad (7.12)$$

are the input and output sensitivity matrices and:

$$T_i = I - S_i = KG(I + KG)^{-1} \quad (7.13)$$

$$T_o = I - S_o = GK(I + GK)^{-1} \quad (7.14)$$

are the input and output complementary sensitivity matrices. Note that, in general, for a MIMO system $KG \neq GK$. Both input and outputs can be weighted using weighting functions, as shown in Figure 7.3.

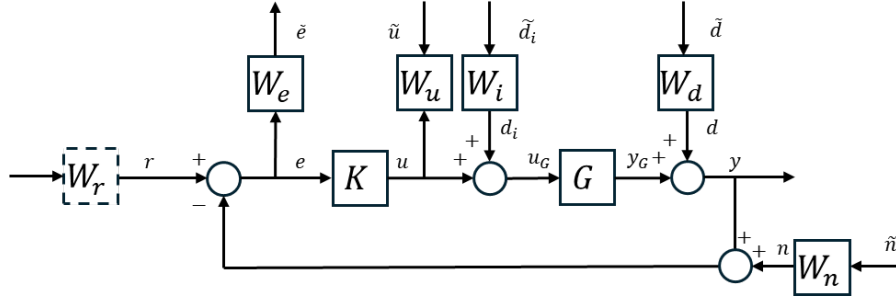


Figure 7.3: Standard feedback configuration with weights

The performance objectives of the closed loop behaviour of the system can be specified in terms of requirements on the sensitivity functions. This can be done conveniently with the use of weight matrices, for instance, for a scalar system, the following condition:

$$\begin{cases} |S(j\omega)| \leq \epsilon, & \forall \omega \leq \omega_0 \\ |S(j\omega)| \leq M, & \forall \omega > \omega_0 \end{cases} \quad (7.15)$$

Can be equivalently written as:

$$|W_e(j\omega)S(j\omega)| \leq 1, \quad \forall \omega \quad (7.16)$$

with:

$$|W_e(j\omega)| = \begin{cases} 1/\epsilon, & \forall \omega \leq \omega_0 \\ 1/M, & \forall \omega > \omega_0 \end{cases} \quad (7.17)$$

where ω is the angular frequency, ϵ and M are scalar numbers used to bound the sensitivity function, ω_0 is a threshold frequency and W_e is the shaping weight.

To find the K which allows us to satisfy the performance objective, different criteria can be used, for instance a mixed criterion on the error and control effort is:

$$\sup_{\|\mathbf{d}\| \leq 1} \{ \|\mathbf{e}\|_2^2 + \rho^2 \|\mathbf{u}\|_2^2 \} = \left\| \begin{bmatrix} W_e S_o W_d \\ \rho W_u K S_o W_d \end{bmatrix} \right\|_\infty^2 \quad (7.18)$$

When we deal with an uncertain system and $G = G_{nom}$ nominal plant, we call this condition **Nominal Performance (NP)**.

7.1.3 Interconnected system

We introduce here the concept of **Linear Fractional Transformations (LFT)**. Let M be a complex matrix partitioned as:

$$M = \begin{bmatrix} M_{11} & M_{12} \\ M_{21} & M_{22} \end{bmatrix} \in \mathbb{C}^{(p_1+p_2) \times (q_1+q_2)}, \quad (7.19)$$

and let $\Delta_l \in \mathbb{C}^{q_2 \times p_2}$ and $\Delta_u \in \mathbb{C}^{q_1 \times p_1}$ be two complex matrices. We can define a lower LFT with respect to Δ_l as:

$$\mathcal{F}_l(M, \Delta_l) := M_{11} + M_{12}\Delta_l(I - M_{22}\Delta_l)^{-1}M_{21} \quad (7.20)$$

and an upper LFT with respect to Δ_u as:

$$\mathcal{F}_u(M, \Delta_u) := M_{22} + M_{21}\Delta_u(I - M_{11}\Delta_u)^{-1}M_{12} \quad (7.21)$$

provided that the inverse matrices in the equations exist. It can be shown that the lower LFT and upper LFT are the matrices obtained by closing respectively the lower loop in the left diagram and the upper loop of the right diagram of Figure 7.4.

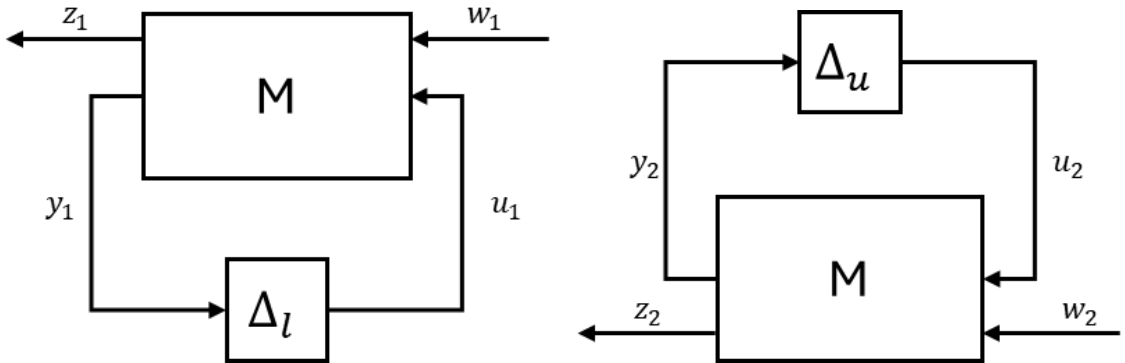


Figure 7.4: Diagram representation of lower LFT (left) and upper LFT (right)

It can be shown that the standard feedback configuration of Figure 7.1 (as any other feedback system) can be rearranged as a LFT, with appropriate choice of matrix P and signals z and w , see Figure 7.5.

When the system is not known exactly but is affected by an uncertainty, the system can be put in the generalised interconnection Δ, P, K -form of Figure 7.6, described by:

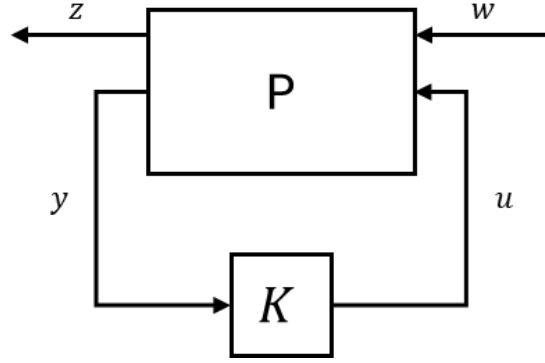


Figure 7.5: LFT representation of the standard feedback configuration

$$\begin{bmatrix} \mathbf{z}_\Delta \\ \mathbf{z} \\ \mathbf{y} \end{bmatrix} = \begin{bmatrix} P_{11} & P_{12} & P_{13} \\ P_{21} & P_{22} & P_{23} \\ P_{31} & P_{32} & P_{33} \end{bmatrix} \begin{bmatrix} \mathbf{w}_\Delta \\ \mathbf{w} \\ \mathbf{u} \end{bmatrix} \quad (7.22)$$

where P is the nominal plant augmented with performance weights, Δ is the uncertainty set, K is the controller, $\mathbf{w}$ is the external input, $\mathbf{u}$ is the control input, $\mathbf{z}$ is the performance output, $\mathbf{y}$ is the measurement output for feedback. The perturbations to the nominal model are assumed to be finite dimensional linear time invariant system.

This is the general framework for system robustness. The concept of robust stability and performance can be introduced making use of the structured singular value μ .

7.1.4 Structured singular value, robust stability and performance

Let's assume that the perturbation matrix Δ is made of two types of block: repeated scalar blocks and full blocks. We define $\Delta \subset \mathbb{C}^{n \times n}$ as:

$$\Delta = \{\text{diag}[\delta_1 I_{r_1}, \dots, \delta_s I_{r_s}, \Delta_1, \dots, \Delta_F] : \delta_i \in \mathbb{C}, \Delta_j \in \mathbb{C}^{m_j \times m_j}\} \quad (7.23)$$

where S is the number of repeated scalar blocks and F is the number of full blocks. For $M \in \mathbb{C}^{n \times n}$ the **structured singular value** $\mu_\Delta(M)$ is defined as:

$$\mu_\Delta(M) := \frac{1}{\min \{\bar{\sigma}(\Delta) : \Delta \in \Delta, \det(I - M\Delta) = 0\}} := \frac{1}{\alpha_{\min}} \quad (7.24)$$

unless no $\Delta \in \Delta$ makes $I - M\Delta$ singular, in which case $\mu_\Delta(M) = 0$. The struc-

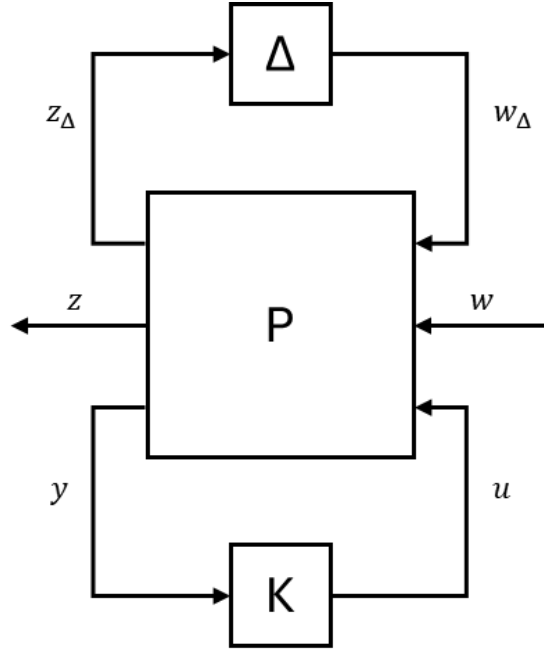


Figure 7.6: Generalised interconnect plant configuration

tured singular value is the reciprocal of the smallest $\alpha = \alpha_{\min}$ for which the $\det(I - M\Delta) = 0$, i.e. smallest destabilizing perturbation. For the general case in which Δ has both scalar and full blocks, μ cannot be exactly determined but only bounded.

Let Δ^* denote the set of all block diagonal and stable rational transfer functions that have block structures such as Δ . Let's consider the following system, where $\mathbf{z} = N_{zw}\mathbf{w}$ and $N_{zw} = F_u(N(P, K), \Delta)$, $N(P, K) = F_l(P, K)$:

The resulting system satisfies **Robust Stability (RS)** if the controller K within the feedback N stabilises the system for all $\Delta \in \Delta^*$, which is $\det[I - M\Delta] \neq 0$, where $M = N_{11}$.

It can be shown that this condition is satisfied if $\mu_\Delta(M) \leq 1$ for all $\Delta \in \Delta^*$. More generally, the system is stable for all $\Delta \in \Delta^*$ with $\|\Delta\|_\infty < \frac{1}{\beta}$ if and only if $\sup_{\omega \in \mathbb{R}} \mu_\Delta(M) \leq \beta$.

Robust Performance (RP) refers to the ability of the closed loop system to satisfy the performance criteria in presence of uncertainty. This can be expressed in general as:

$$\|F_u(N, \Delta)\|_\infty \leq \gamma, \quad \|\Delta\|_\infty < \frac{1}{\gamma} \quad (7.25)$$

It can be shown that the RP can be transformed into a RS introducing a fictitious unstructured uncertainty Δ_f of dimensions corresponding to the size of $\mathbf{z}$ and $\mathbf{w}$ and to solve the equivalent robust stability problem of the resulting interconnection.

Hence the RS and RP analysis can be reconducted to the evaluation of the structured singular value μ .

7.1.5 Robust control synthesis technique and tools

The synthesis of controller which satisfy NP, RS and RP can be reconducted to the evaluation of:

$$\|M\|_\alpha, \alpha = 2, \infty \text{ or } \mu \quad (7.26)$$

for some transfer matrix M ($\|M\|_\mu$ is a short-hand notation to indicate the peak of the structured singular value across frequencies).

The synthesis framework is represented in Figure 7.7,

where $M = \mathcal{F}_l(G, K) = \begin{bmatrix} G_{11} & G_{12} \\ G_{21} & G_{22} \end{bmatrix}$

and $G = \begin{bmatrix} G_{11} & G_{12} \\ G_{21} & G_{22} \end{bmatrix}$, which is chosen respectively as:

- $G = \begin{bmatrix} P_{32} & P_{33} \\ P_{32} & P_{33} \end{bmatrix}$ for NP only, i.e. $\Delta = 0$;
- $G = \begin{bmatrix} P_{11} & P_{13} \\ P_{31} & P_{33} \end{bmatrix}$ for RS only;
- $G = P = \begin{bmatrix} P_{11} & P_{12} & P_{13} \\ P_{21} & P_{22} & P_{23} \\ P_{31} & P_{32} & P_{33} \end{bmatrix}$ for RP

Each case then leads to the synthesis problem:

$$\min_K \|\mathcal{F}_l(G, K)\|_\alpha \quad \text{for } \alpha = 2, \infty \text{ or } \mu \quad (7.27)$$

The μ -synthesis optimisation problem is non-convex. Algorithms exist, one of these is the D-K iteration process which involves scaling of the system with appropriate matrices and solving the $\mathcal{H}_\infty$ problem on this scaled system.

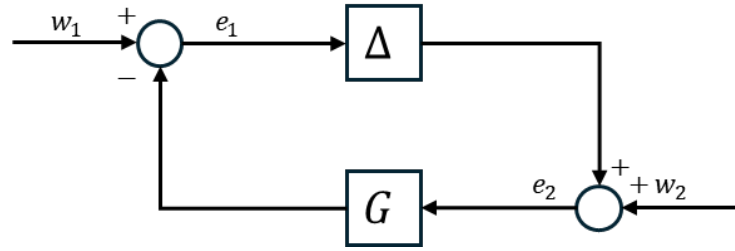


Figure 7.7: General framework for the robust control synthesis problem

The following MATLAB functions can be used for the analysis and synthesis problem:

- NP can be measured using the function `hinfnorm` (The MathWorks Inc., 2025b);
- RS and RP can be tested using respectively the MATLAB functions `robstab` (The MathWorks Inc., 2025e) and `musynperf` (The MathWorks Inc., 2025d);
- full order controllers which satisfies NP can be synthesised using the function `hinfysn` (The MathWorks Inc., 2025b);
- full order controllers and structured controllers which satisfies RP can be synthesised using the function `musyn` (The MathWorks Inc., 2025c);
- structured controllers which satisfy NP and RS for real uncertainty can be synthesised using the function `systeme` (The MathWorks Inc., 2025f);

8

Methods

The methodology adopted in Part II of the thesis is presented in this chapter. Section 8.1 introduces the ESA high-performance stabilisation breadboard. section 8.2 presents the non-dimensional normalisation used to report the results of Part II of the thesis. Section 8.3 presents the system identification methodology while Section 8.4 the controller synthesis methodology.

8.1 The ESA high-performance stabilisation breadboard

ESA has developed the high-performance stabilisation breadboard which aims at mitigating microvibration generated by spacecraft equipment such as wheels or cryo-coolers. The breadboard employs 8 actuators (i.e. shakers) to actively reduced the vibration on-board of the satellite. 6 of these shakers are used for control while the other 2 are used to inject disturbance at a fixed or varying frequency. This allows to simulate the effect of on-board disturbances generated by wheels or cryo-coolers. An accelerometer is set at the edge of a beam in a cantilever configuration. The beam length can be varied to simulate different payloads. The length of the beam indeed determines its vibration mode frequency.

8.2. Presentation of data in non-dimensional form

Figure 8.1 shows simplified architectural diagram of the ESA high-performance breadboard

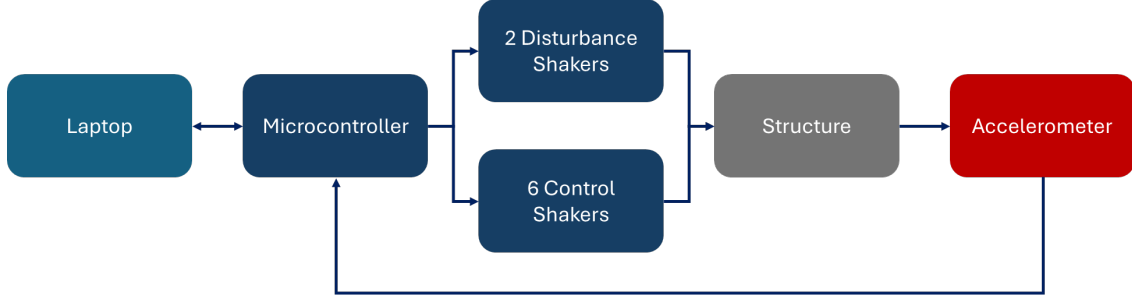


Figure 8.1: Simplified architectural diagram of the ESA high-performance breadboard

The breadboard can be used to run system identification experiments. The collected data is processed in MATLAB to perform the full system identification and obtain the Frequency Response Function (FRF) of the platform. The FRF can be used to obtain a parametric model of the platform or to synthesise data-driven controllers. The parametric model can be used to synthesise model-based controllers. The controller synthesis is done through MATLAB. The controller is then discretised and deployed on the breadboard, where it can be tested.

8.2 Presentation of data in non-dimensional form

Part of the work performed at ESA ESTEC facility is protected by a non-disclosure agreement. The data in this part of the thesis is presented in non-dimensional form to preserve its confidentiality. The following non-dimensional ratios are used:

- The frequency is made non-dimensional using the system characteristic frequency f_c equal to the system largest resonant peak: $\tilde{f} = f/f_c$;
- The time is made non-dimensional using the system characteristic period $T_c = 1/f_c$: $\tilde{T} = T/T_c$;
- The acceleration is made non-dimensional using the saturation value a_{max} : $\tilde{a} = a/a_{max}$

In the following, the tilde notation is drop and it is given that the represented quantities (frequency, acceleration, time) are in non-dimensional form.

8.3 System identification

The advanced control techniques employed for the design of the controller for the breadboard ($\mathcal{H}_\infty$ -, μ -synthesis, etc.) are model-based, thereby necessitating an accurate dynamic representation of the system. There are different ways in which a model of the plant can be obtained, techniques based on physical insights are distinguished from data-driven methods. First principles modelling relies entirely on physical insight and fundamental laws to derive the governing equations describing the system dynamics. A black-box data-driven method, instead, operates without a priori knowledge of the system's internal physical properties, constructing the model entirely from experimental input/output data. Finally, a mixed approach consist in identifying unknown parameters of a first-principles based model through empirical data. Previous work on the breadboard compared an accurate physical modelling with a black-box approach with the latter resulting in much more accurate plant description (TU/e, 2024). Furthermore, a physical modelling of the platform should be repeated every time its configuration is changed, which can result in a long and tedious process, data-driven methods instead are highly flexible in this perspective. Hence, a black-box method is used to derive an accurate model of the plant dynamics.

The system identification process is structured as follows. First, the FRF of the system is estimated based on experimental data obtained by exciting the system at different frequencies, this step is called non-parametric identification. Then a state-space model is obtained by fitting the FRF, this step is called parametric identification. Finally, the modelling error is incorporated in the description of the system uncertainty. The three steps are described in Section 8.3.1, Section 8.3.2 and Section 8.3.3.

8.3.1 Non-parametric identification

The first step in the non-parametric identification is the design of the excitation signal. In general, when evaluating the frequency response of a windowed signal transient leakage effect will be present. The leakage error can be removed if using periodic signal. A random-phase multisine signal can be defined to excite different harmonics (Pintelon and Schoukens, 2012). The drawback when compared to random noise is a reduction in the frequency resolution (Schoukens

8.3. System identification

et al., 2018). Compared to a swept sine or chirp excitation instead, no power is present outside of the frequency band of interest (Schoukens et al., 2018).

The algorithm used to identify the dynamics of the vibration isolation platform is the so-called Local Polynomial Method (LPM). The LPM methodology has two methods to estimate the FRF of the system; a robust and a fast method (Pintelon and Schoukens, 2012). When this method is applied for data generated by using periodic excitation signal only the noise transient term is approximated using polynomials and can be hence subtracted (Pintelon et al., 2011). Here an implementation of the robust method is used. Because of the excessive coupling between the eight actuators a “column-wise” approach is followed in which every shaker is excited individually. The excitation signal is a random-phase multi-sine of P periods each having a fixed duration and with time step. The signal is repeated to form N blocks.

In implementing the LPM algorithm, the user can freely select the following parameters:

- Order R of local polynomial: a study on the polynomial approximation error of a second-order system found that the error does not decrease by passing from a polynomial of even order to one of odd order (Schoukens et al., 2013). Hence, it is more efficient to use polynomial of even order. The bias error of the covariance estimate results from the sum of different contributions, some which increase with R and other which decrease with R . As reported in Pintelon and Schoukens (2012), a second order polynomial approximation $R = 2$ is often a good compromise but for lowly damped systems larger values ($R = 3, 4$) are sometimes needed, especially around the resonance frequency;
- Number of realisation M . If $M \geq 2$ the stochastic spectral nonlinear distortion can be estimated and separated from noise (Pintelon and Schoukens, 2012);

In the practical implementation, the order of the local polynomial is varied in-between 2 and 4 to assess the impact it has on the FRF estimate while the number of realisations is fixed to 2.

8.3.2 Parametric identification

For model based controller design a parametric model must be obtained starting from the FRF. Among the methods that have been developed, a modified version of the one described in [de Callafon et al. \(1996\)](#) is employed. The original method estimates a linear multivariable model on the basis of FRF. The estimated model is parametrised using either a left or a right polynomial matrix fraction description. The parameters are computed by using a two-norm minimisation of a multivariable (Schur weighted) output error. In the version modified by the same author and implemented in the function `bclsfits`, the fitting is done by first prewarping the frequency vector according to the ‘tustin’ or bilinear discrete-time equivalent of the continuous-time system and then fit a discrete-time model that is converted back to continuous-time via an inverse bilinear transformation. This is reported to greatly improve the numerical conditioning of the curve fitting problem.

The function requires as inputs the FRF obtained from the nonparametric identification, the weighting function and the model order. It gives as output the state space description of the fitted model. The model order is varied at fixed steps in a given range to evaluate the fitting accuracy. This is evaluated visually by looking at the bode magnitude response of the FRF and of the fitted models. For a MIMO system this comparison however can be difficult since it requires comparing $n_y \times n_u$ plots, where n_y is the number of outputs and n_u the number of inputs. To obtain a single error metrics at different frequency, the largest singular value of the difference between the FRF and the fitted model is used.

As it will be presented in the following, the system dynamics is characterised by many modes and requires a high number of parameters for an accurate description. The number of parameters of the model has an impact on the number of parameters of the controller. For a controller to be implemented on a satellite hardware, or more in general on an embedded hardware, the number of parameters should, however, be limited. For model based controller synthesis, the controller order is proportional to the number of states of the plant (equal in case of H-infinity synthesis). Reducing the order of the plant thus goes into the direction of obtaining a reduced order controller. The Balanced stochastic model truncation (BST) via Schur method is used here for model reduction. It is implemented by the MATLAB function `bstmr` ([The MathWorks Inc., 2023](#)). The key idea be-

hind the balanced truncation method is to reduce states that hard to observe and control. To do so, a balanced realisation of the system is obtained, for which the observability and controllability Gramians are diagonal and equal. The square roots of the singular values of the product of the observability and controllability Gramians are the Hankel singular values of the system and provide a measure of the energy of the system. States with higher energy are retained. The method also provide a bound for the error made by approximating the original system with the reduced one (Zhou and Doyle, 1999).

Also for the model reduction step the accuracy of the fitting is assessed by looking at the bode magnitude plot of the original and reduced model and at the largest singular value of the difference model.

8.3.3 Uncertainty modelling

When modelling mathematically a real system a modelling error is introduced. This is the case for the nonparametric and parametric modelling of the bread-board described above. The FRF obtained by experimental data are already erroneous because of sensor errors, non-controlled environment and limited bandwidth. The parametric identification introduces a further modelling error. Finally, low energy modes are eliminated by model reduction introducing a further error. The modelling error is one of the sources of uncertainty in the knowledge of the system.

There exist different possible representation of uncertainty, additive uncertainty and input or output multiplicative uncertainty are widely used (Zhou and Doyle, 1999) and are respectively described by Equation (8.1), Equation (8.2) and Equation (8.3).

$$G = G_{nom} + W_1 \Delta W_2 \quad (8.1)$$

$$G = G_{nom}(I + W_1 \Delta W_2) \quad (8.2)$$

$$G = (I + W_1 \Delta W_2)G_{nom} \quad (8.3)$$

An additive uncertain model can be used to characterise additive plant error, neglected high frequency dynamics and uncertain right-half plane zeros location. A multiplicative model can be used to represent neglected high frequency

dynamics and uncertain right-half plane zeros location. Additionally, an input multiplicative model can be used to represent input (actuators) errors, an output multiplicative model can be used to represent output (sensors) errors (Zhou and Doyle, 1999). All three types of uncertainty model are supported by the MATLAB function `ucover` (The MathWorks Inc., 2025g) which can be used to fit an uncertain model to a set of LTI responses. The function requires as input the nominal model G_{nom} and a set of LTI models G_{array} to be covered by the uncertainty. It measures the gap between G_{nom} and G_{array} at each frequency on a grid, and select shaping filters whose magnitude approximates the maximum gap. To design the minimum-phase shaping filters W_1 and W_2 , the `ucover` command performs two steps:

1. Compute the optimal values of $W_{1,opt}$ and $W_{2,opt}$ on a frequency grid;
2. Fit W_1 and W_2 values with the dynamic filters of the specified orders using `fitmagfrd` (The MathWorks Inc., 2025a), a function which fits frequency response magnitude data with minimum-phase state-space model using log-Chebyshev magnitude design.

In our case G_{nom} will be the reduced order parametric model while G_{array} will be the full order parametric model, representing the true plant dynamics. One could use the FRF instead of a parametric model but the result will not be in state space form and it will not be useful for controller synthesis. The order of the shaping weight is found iteratively. A too low order may result in a very conservative uncertainty, making controller synthesis challenging. A high order will increase the number of states of the uncertain model, which could in turn result in a higher order controller. Ideally the uncertainty should be representative of two facts:

1. In the frequency band where the frequency response is well known there is a modelling error due to parametric fitting and model order reduction;
2. At frequencies outside this band the plant response estimate is noisy or unknown and a conservative estimate should be made, i.e. the response should cover the noise level.

The second fact is taken into account by manually adding a flat response at low and high frequencies of G_{array} such that the response is different from the one of

G_{nom} and by letting the ucover function cover this difference with a greater uncertainty. An input multiplicative model is used for the uncertainty description. Output multiplicative and additive uncertainty representation were also tested but resulted in too conservative results. The input multiplicative model is represented in Figure 8.2, where $G_n=G_{nom}$ is the nominal plant, Δ is the uncertainty block, W_1 and W_2 are weights that shapes the uncertainty.

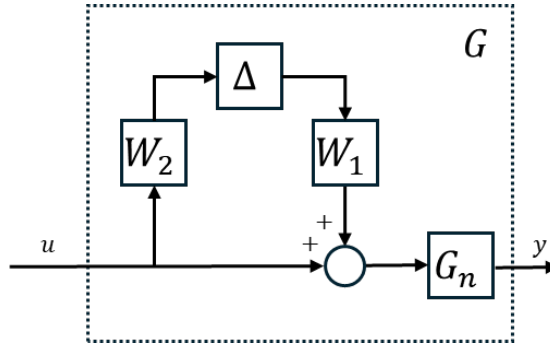


Figure 8.2: Plant with input multiplicative uncertainty

8.4 Controller synthesis

To formulate the controller synthesis problem, we use the standard feedback configuration of Figure 7.1. In our case, it is assumed that a disturbance $\mathbf{d}$ acts at the output of the system, hence $\mathbf{d}_i = \mathbf{0}$. In the experimental setup the disturbance is instead an input disturbance. The disturbance is in fact created by actuator 7 or 8, but we take advantage of the fact that we know how the disturbance is transferred to the output by the plant thanks to the FRF experimental data, so we know what level of output disturbance will be measured if the disturbance actuator are driven with a given voltage. Additionally, considering an output disturbance resembles more what would happen when an unknown payload, such as a reaction wheel set, is mounted on the breadboard: the disturbance is directly measured at the output. Also, we consider $\mathbf{n} = \mathbf{0}$, since $d \gg n$.

With these assumptions, we can write:

$$\mathbf{y} = T_o \mathbf{r} + S_o \mathbf{d} \quad (8.4)$$

$$\mathbf{r} - \mathbf{y} = S_o (\mathbf{r} - \mathbf{d}) \quad (8.5)$$

$$\mathbf{u} = KS_o(\mathbf{r} - \mathbf{d}) \quad (8.6)$$

$$\mathbf{u}_p = \mathbf{u} \quad (8.7)$$

If we focus on Equation (8.5) and Equation (8.6), we see that both the error signal $\mathbf{e} = \mathbf{r} - \mathbf{y}$ and the input signal $\mathbf{u}$ depend on the signal $\mathbf{r} - \mathbf{d}$, the first through the sensitivity function S_o and the second through the control sensitivity function KS_o (the name control sensitivity function is used for KS following the notation of [TU/e \(2024\)](#)).

Performance weighting can be used to shape S_o and KS_o to achieve desired objectives in stability and performance ([Zhou and Doyle, 1999](#)). We modify the feedback configuration of Figure 7.1 to include performance weighting W_e and W_{KS} , as shown in Figure 8.3. In this case, we have:

$$\tilde{\mathbf{e}} = W_e \mathbf{e} = W_e S_o(\mathbf{r} - \mathbf{d}) \quad (8.8)$$

$$\tilde{\mathbf{u}} = W_{KS} \mathbf{u} = W_{KS} KS_o(\mathbf{r} - \mathbf{d}) \quad (8.9)$$

Since the W_e weight is effectively used to shape the sensitivity function, we pose $W_S = W_e$. For the LTI SISO controller, W_S is designed to make S_o small at the frequency of the disturbance such that the effect of $\mathbf{d}$ on $\mathbf{e}$ is small; W_{KS} is designed to limit the actuator response and to impose rolloff at high frequency. More specifically, W_S is designed such that:

- It has a specified base level attenuation for stability;
- It has a notch at a specified frequency and with given value of damping factor.

W_{KS} is designed such that:

- At low frequency in the control bandwidth it has a value proportional to the allowed actuator response and inversely proportional to the maximum value of the disturbance signal;
- At high frequency, in which the response of the plant is unknown, it rolloffs to a small value.

8.4. Controller synthesis

For the LTI MIMO controller, the diagonal entries of the sensitivity and control sensitivity matrices are shaped using the same weights of the LTI SISO controller. From the implementation point of view, the frequency of the notch is defined as uncertain real parameter using the MATLAB function `ureal` with nominal value equal to the frequency at which the disturbance acts and a variation of 0.2% for LTI controller, or a variation within a specified range for LPV controllers. The base level attenuation is needed to ensure that a specified level of gain margin at high frequencies is obtained. For instance a gain margin of 6 dB can be enforced for stability (Ogata, 2010). The notch is needed for good disturbance rejection at the disturbance frequency. A peak in the weight function corresponds to a very low value of sensitivity at the given frequency which make the effect of the disturbance on the input small (Zhou and Doyle, 1999).

Henceforth, it is given that $S = S_O$ and $KS = KS_O$, unless otherwise noted.

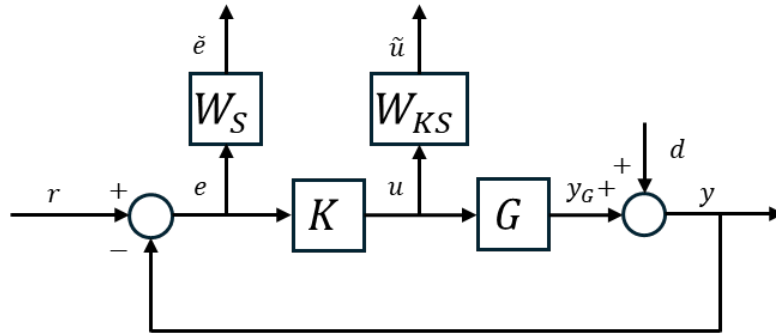


Figure 8.3: Standard feedback configuration with weights

As introduced earlier, the plant G contains uncertain dynamics modelled by $W_1\Delta W_2$. We would like the controller to be designed taking into account the uncertainty in the plant model. Robust stability and performance criteria can be defined for a system with model uncertainty.

8.4.1 LTI controllers design using robust control techniques

Using shaping weights, two types of SISO and MIMO controllers are synthesised:

- Low order LTI controller which achieves NP and RS;
- Low order LTI controller which achieves RP and RS;

In the case of the LTI controller which achieves NP and RS the following procedure is adopted:

- A full order controller is synthesised which achieves NP using the MATLAB function `hinfsyn`;
- RS is checked using the MATLAB function `robstab`;
- If NP or RS are not achieved the performance requirements are relaxed;
- The controller order is reduced to the minimum order which achieves both NP and RS using the MATLAB function `reduce`;
- A structured controller of a desired order is synthesised using the MATLAB function `sys tune` and using the controller synthesised at the previous step as initial guess;
- If the controller does not achieve NP and RS the desired order is increased up to a user defined maximum order;
- If the controller of highest order allowed does not achieve NP and RS the performance requirements are relaxed;

Using `sys tune` with a low order controller as initial guess reduces the convergence time with respect to using `sys tune` directly from the beginning. The procedure is summarised in Figure 8.4. The diagram is simplified for clarity.

A similar procedure is used to synthesise a LTI controller which achieves both RS and RP:

- A full order controller is synthesised which achieves RP and RS using the MATLAB function `musyn`;
- If RP or RS are not achieved the performance requirements are relaxed;
- The controller order is reduced to the minimum order which achieves both RP and RS using the MATLAB function `reduce`;
- A structured controller of desired order is synthesised using the MATLAB function `musyn` and using the controller synthesised at the previous step as initial guess

8.4. Controller synthesis

- If the controller does not achieves RP or RS the desired order is increased up to a user defined maximum order;
- If the controller of highest order allowed does not achieve NP and RS the performance requirements are relaxed.

Also here using the function `musyn` with an initial guess reduces the convergence time. `musyn` is used instead of `systune` because this last one can handle only uncertain real parameters, while the dynamics uncertain modelling includes, in general, complex parameters. The procedure is summarised in Figure 8.5. The diagram is simplified for clarity. In addition to the call of `systune` and `musyn` with an initial guess, these functions are called other three times with random initialisation of parameters, to mitigate the effects of local minima in the optimisation.

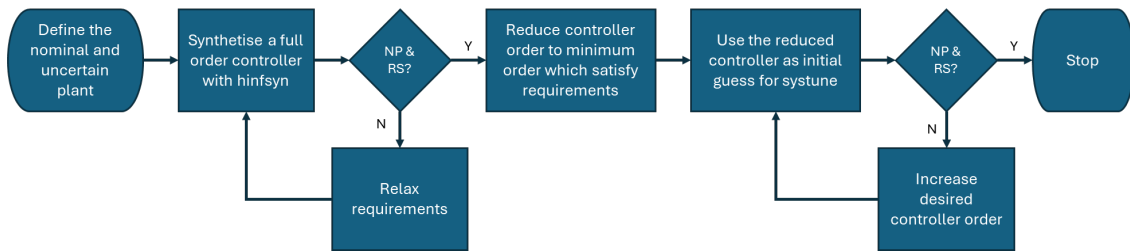


Figure 8.4: Flowchart describing the process for obtaining a low order LTI controller which satisfies NP and RS. Y=Yes, N=No

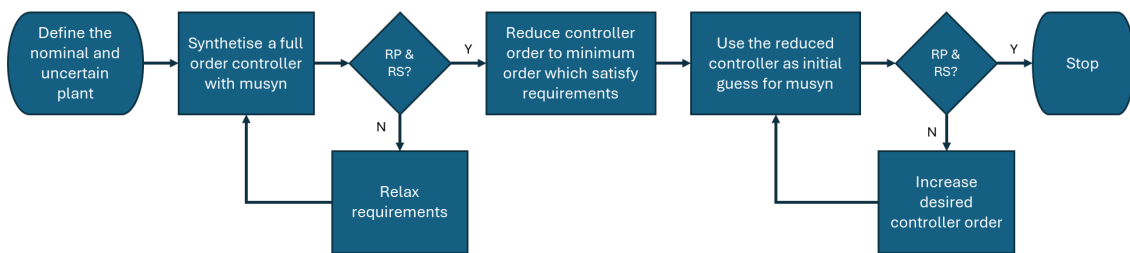


Figure 8.5: Flowchart describing the process for obtaining a low order LTI controller which satisfies RP and RS. Y=Yes, N=No

8.4.2 LPV controllers design using robust control techniques

For systems in which the dynamics is time-dependent the Linear Parametric Varying (LPV) is a useful tool for control design. The breadboard on the current

configuration can be well described as a LTI system. The disturbance introduced by the shaker can, however, be time-varying to simulate the disturbance created by reaction wheel unbalance, which depends on the reaction wheel speed. The LPV framework is then useful to synthesise a controller which can reject this time-varying disturbance.

The generalised plant interconnection of Figure 7.6 can be modified to include the scheduling variable p , which makes the controller LPV. If the shaping weights also depend on p , then the generalised plant is LPV as well (see Figure 8.6). The controller dynamics is described in state space representation by:

$$\begin{bmatrix} \dot{\mathbf{x}}_K \\ \mathbf{u} \end{bmatrix} = \begin{bmatrix} A_K(p) & B_K(p) \\ C_K(p) & D_K(p) \end{bmatrix} \begin{bmatrix} \mathbf{x}_K \\ \mathbf{y} \end{bmatrix} \quad \mathbf{w}_K = \Delta_K(p) \mathbf{z}_K \quad (8.10)$$

where the controller state space matrices A_K, B_K, C_K, D_K are dependent on the scheduling variable p . The controller can be formulated in Linear Fractional Representation (LFR) as:

$$\begin{bmatrix} \mathbf{z}_K \\ \mathbf{u} \end{bmatrix} = \mathcal{F}_u \left(\begin{bmatrix} A & B_w & B_y \\ C_z & D_{zw} & D_{zy} \\ C_u & D_{uw} & D_{uy} \end{bmatrix}, \Delta_K(p) \right) \begin{bmatrix} \mathbf{w}_K \\ \mathbf{y} \end{bmatrix} \quad (8.11)$$

where $\mathbf{w}_K$ and $\mathbf{z}_K$ are latent variables which define the dependency of K on $\Delta_K(p)$.

To synthesise a structured controller, the MATLAB function `systune` is used. p is defined as an uncertain real parameter $p \in [p_{MIN}, p_{MAX}]$ instead of a time-varying variable $p(t)$, the plant is thus modelled as uncertain system and a robust controller is synthesised. The synthesised controller will guarantee performance for frozen values of p in the set $[p_{MIN}, p_{MAX}]$ and there is no guarantees performance are met for a time varying $p(t)$. The actual performance should be verified in simulation and experimentally.

The structured controller synthesis is initialised with the full order controller obtained using the function `hinfyn`, but also other three parallel runs of the function are performed with random initialisation. The order of the controller and the dimension of the scheduling variable are gradually increased until RP and RS are met. It must be noted that the function `systune` can only handle uncertain real parameter, hence the concept of RP is relative to the uncertainty

8.4. Controller synthesis

in the shaping weight notch frequency but not to the uncertain plant dynamics. The function `musyn` could be used to synthesise a controller which achieves RP also with respect to the uncertainty in the plant dynamics, the use of this function however systematically failed in obtaining a RP controller. For this reason, the function `systune` is used and just RS is checked using the function `robustab`. Figure 8.7 schematises the procedure followed to synthesise a low order LPV controller.

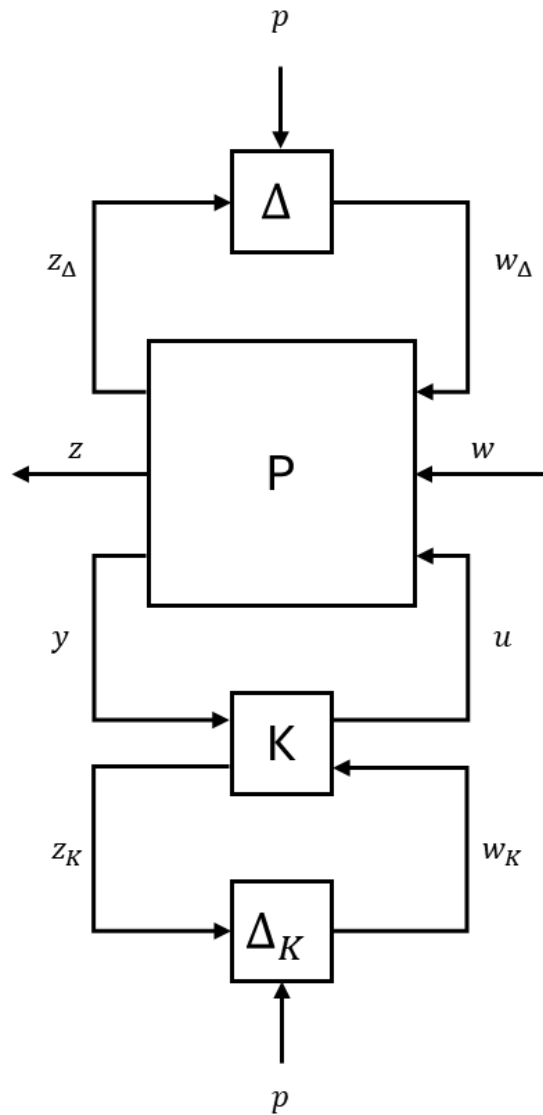


Figure 8.6: Generalised interconnect plant configuration with scheduling parameter p

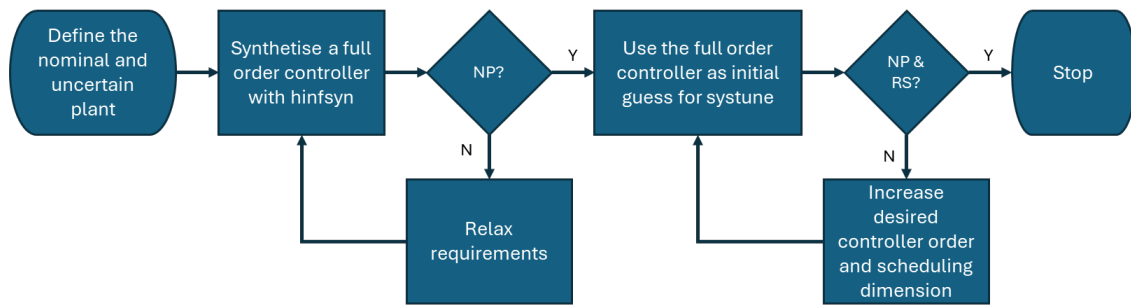


Figure 8.7: Flowchart describing the process for obtaining a low order LPV controller which satisfies NP and RS. Y=Yes, N=No

9

Results

This chapter presents the results obtained in Part II of the thesis. Section 9.1 presents the system identification results while Section 9.2 the controller synthesis and controller test results. Finally, Section 9.3 illustrates the GUIs created to automatise the system identification and controller synthesis procedures.

9.1 System identification

The results of the non-parametric system identification are presented in Section 9.1.1, Section 9.1.2 presents the results of the parametric identification while Section 9.1.3 presents the results of the uncertainty modelling. Section 9.1.4 summarises the system identification results.

9.1.1 Non-parametric identification

The non-parametric identification has been performed for two configurations of the breadboard, differing from each other for the position of the accelerometer on the beam, varied from L_1 to $L_2 = 1.6L_1$. This change in position is expected to change the dynamic response of the breadboard.

9.1. System identification

The FRF of the first experiment is shown in Figure 9.1 and Figure 9.2 where the 99% confidence bound for the FRF based on the estimated noise covariance matrix is also shown. The FRF are clearly visible above the noise in the frequency range from $0.1f_c$ to $1.3f_c$. The areas with most uncertainty in the estimation are the one below $0.1f_c$ and the ones corresponding to the anti-resonance frequencies. Increased uncertainty at low frequencies is typical for accelerometers measurements that are less sensitive to low frequencies, and the gain of the actuator is small at low frequencies. The uncertainty increases at the anti-resonance frequencies because of the decrease in Signal-to-Noise Ratio (SNR). A resonant peak is clearly visible at the frequency f_c . Figure 9.3 and Figure 9.4 show the same FRF with zoom on the $0.1f_c$ to $1.3f_c$ frequency range.

This resonant peak is most probably caused by the resonant frequency of the accelerometer beam. The accelerometer is mounted at the end of a cantilever beam. The first natural frequency of a continuous cantilever beam with a lumped mass at the end can be calculated as (vlab, 2011):

$$\omega_{n1} = \sqrt{\frac{3(1.875)^4 EJ}{3\rho AL^4 + (1.875)^4 m_{acc} L^3}} \quad (9.1)$$

where E is the material Young modulus, J is the moment of inertia of the beam cross section, ρ is the beam material density, A is the beam cross sectional area, L is the beam length and m_{acc} is the mass of the accelerometer mounted at the end of the beam. If one substitutes the value for the beam material and size and for the accelerometer mass, obtains a frequency of $1.1f_c$, which, given the uncertainty in the parameters of Equation (9.1), can be considered close enough to f_c .

Using eq. (9.1) with the new beam length L_2 , the new resonant frequency results to be equal to $0.5f_c$. The FRF for the second experiment is shown in Figure 9.5, Figure 9.6 and in Figure 9.7, Figure 9.8. A few conclusion can be drawn by looking at the figures:

- Considering output 1 and 2, the resonance peak at f_c is not present while there is amplification of the response in between $0.27f_c$ and $0.67f_c$. The increase in the amplification can be attributed to both the shift of the resonance frequency and to the increase in oscillation amplitude of the accelerometer beam because of its greater length. The amplification is much

larger in output 1 than in output 2.

- Considering output 3, the estimation of the FRF is very noisy. This could be caused by accelerometer saturation.

To investigate if the increased beam length led to the saturation of the accelerometer measurement in the time series of the accelerometer data has been plotted and is shown in Figure 9.9. It can be noticed signal clipping for the third output of the accelerometer, which may indicate saturation. The accelerometer readings are however within the range indicated in the datasheet, indicating that saturation is probably not the only effect or is acting at lower acceleration level.

No difference in the FRF estimation was found by using different orders of the local polynomial (2 or 4) and it was set to 2.

From the non-parametric system identification experiments the following conclusions can be drawn:

- The platform dynamics is complex and characterised by many modes in a wide frequency range ($0.1f_c$ to $1.3f_c$). This fact supports the use of data-driven methods as compared to first principle models;
- Depending on the position of the accelerometer on its beam a resonance mode at f_c is present;
- Clipping of the accelerometer signal is present when the accelerometer is mounted at a greater distance on its beam. In general, the position of the accelerometer has a considerable influence on the system dynamics.

In the following, the FRF coming from the first experiment are used.

9.1. System identification

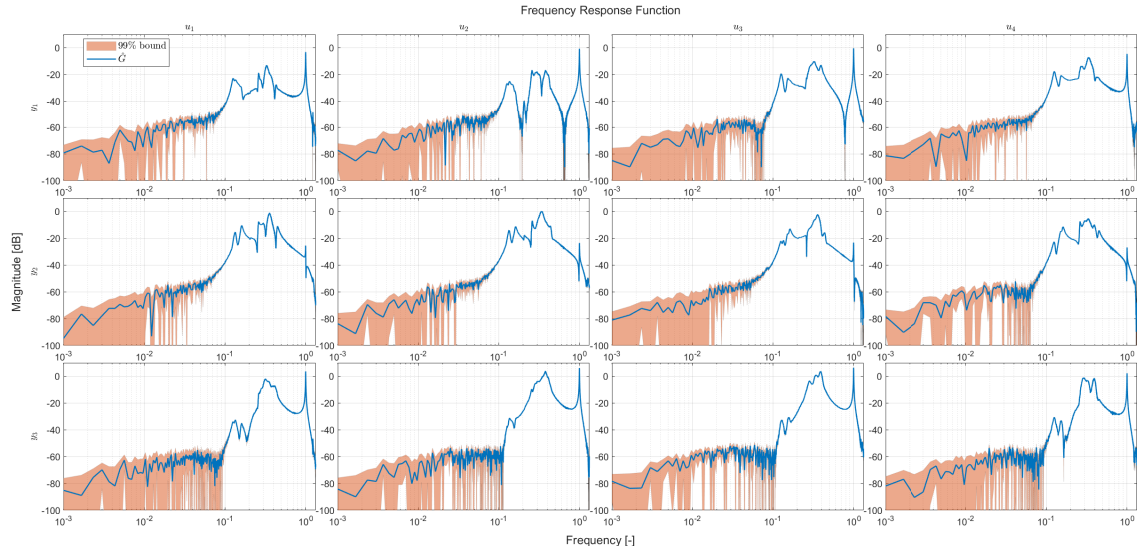


Figure 9.1: Bode magnitude plot of frequency Response Function, inputs 1 to 4, breadboard accelerometer in position 1

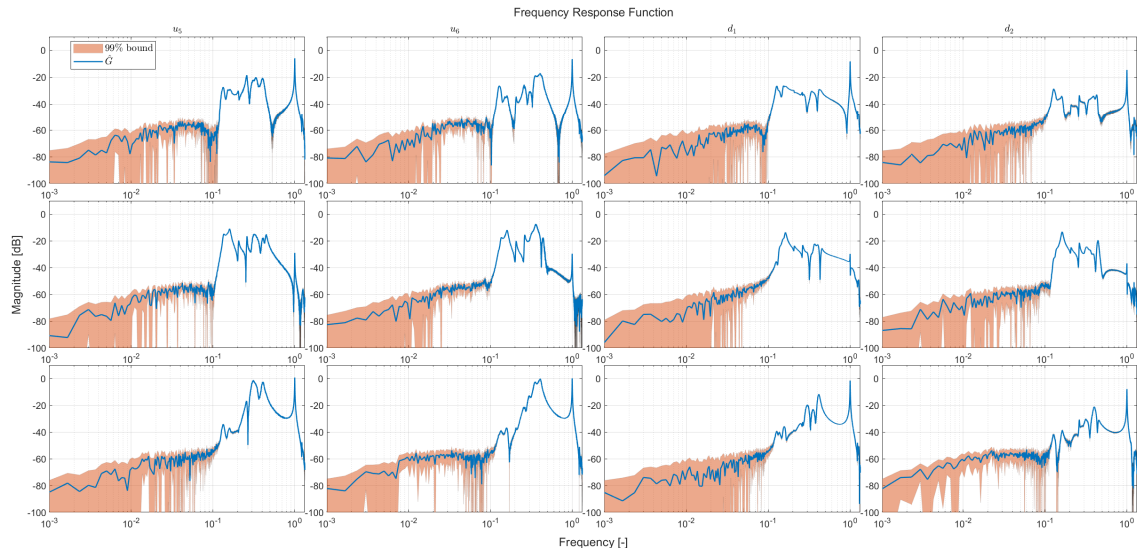


Figure 9.2: Bode magnitude plot of frequency Response Function, inputs 5 to 8, breadboard accelerometer in position 1

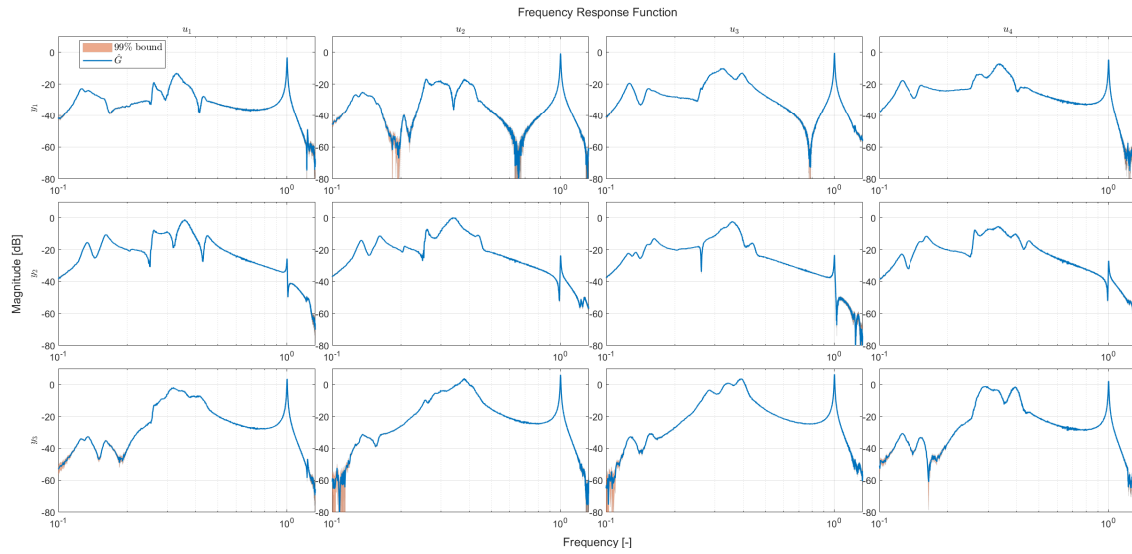


Figure 9.3: Bode magnitude plot of frequency Response Function, inputs 1 to 4, zoom on frequency range, breadboard accelerometer in position 1

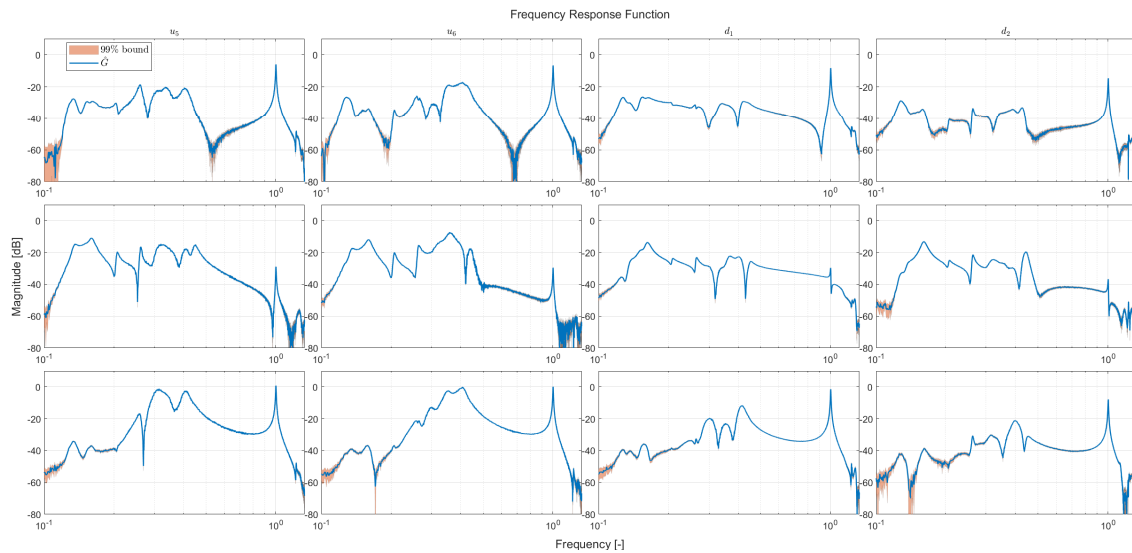


Figure 9.4: Bode magnitude plot of frequency Response Function, inputs 5 to 8, zoom on frequency range, breadboard accelerometer in position 1

9.1. System identification

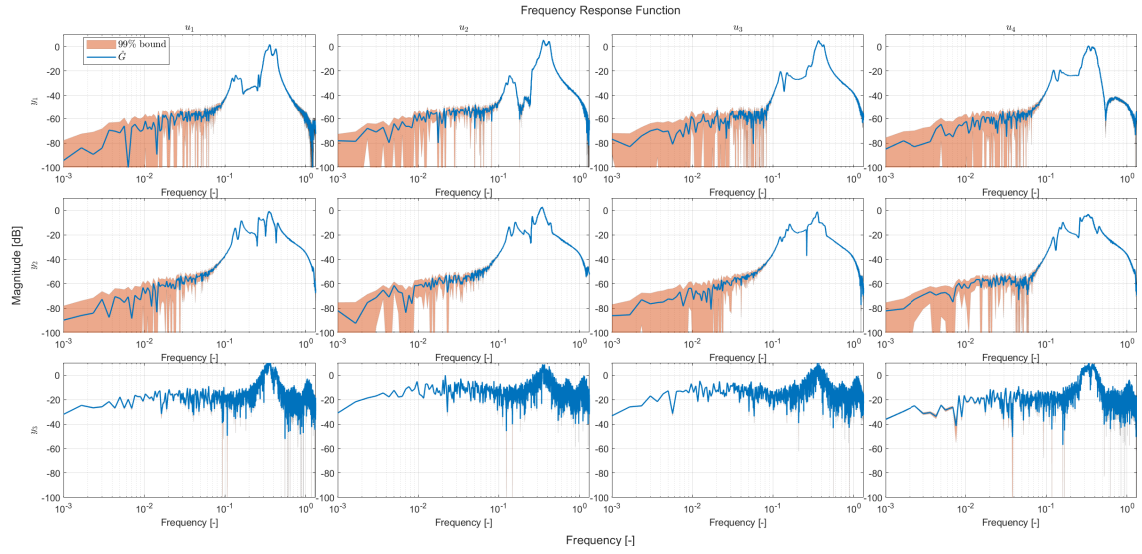


Figure 9.5: Bode magnitude plot of frequency Response Function, inputs 1 to 4, breadboard accelerometer in position 2

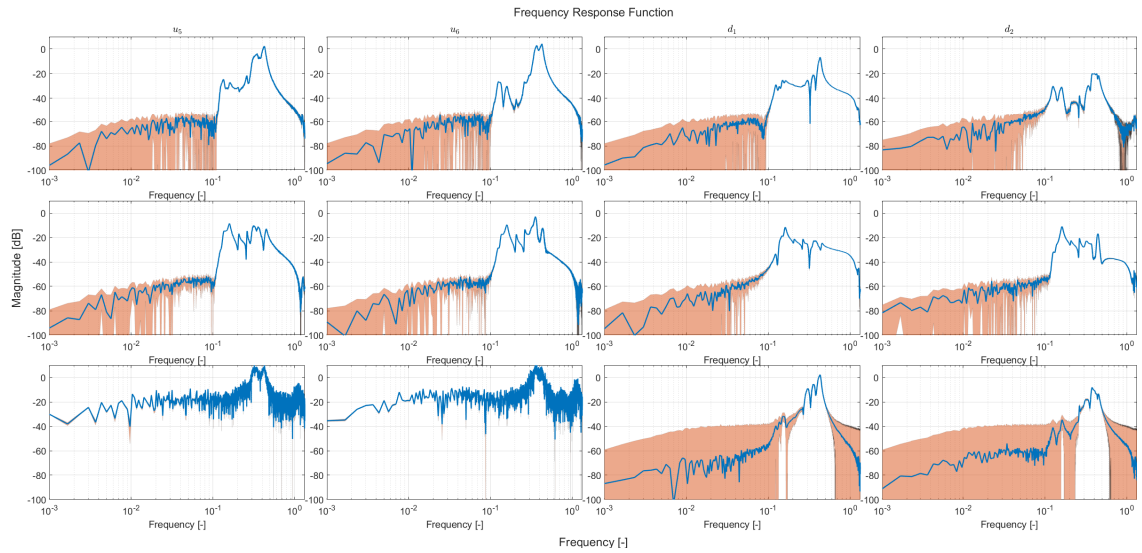


Figure 9.6: Bode magnitude plot of frequency Response Function, inputs 5 to 8, breadboard accelerometer in position 2

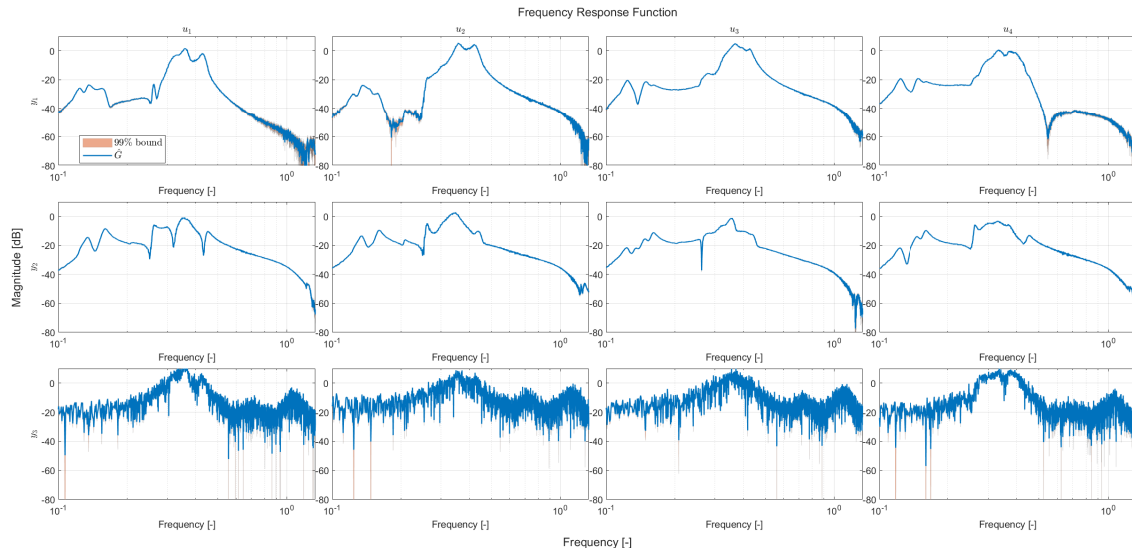


Figure 9.7: Bode magnitude plot of frequency Response Function, inputs 1 to 4, zoom on frequency range, breadboard accelerometer in position 2

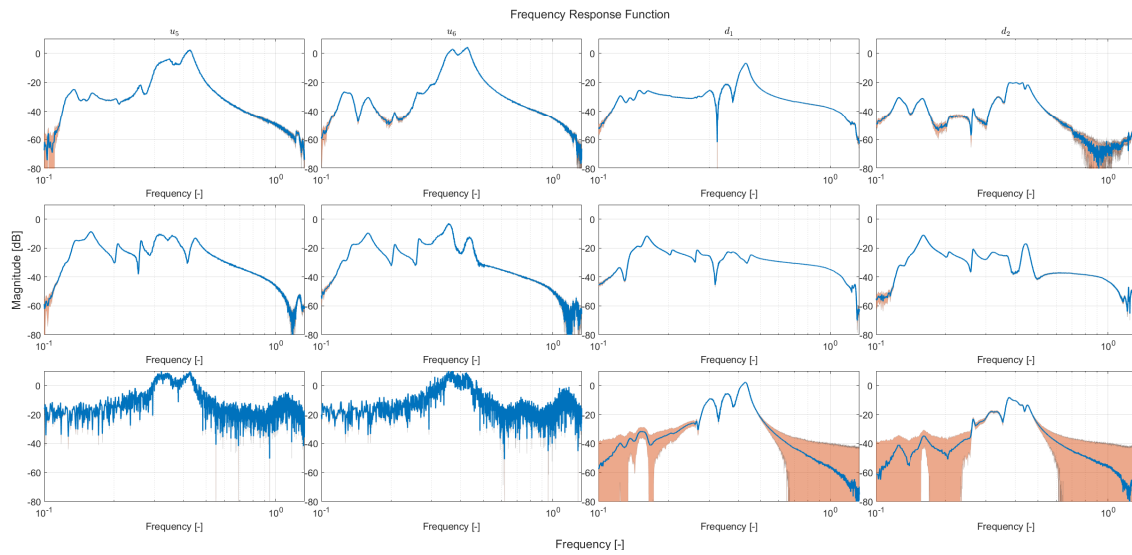


Figure 9.8: Bode magnitude plot of frequency Response Function, inputs 5 to 8, zoom on frequency range, breadboard accelerometer in position 2

9.1. System identification

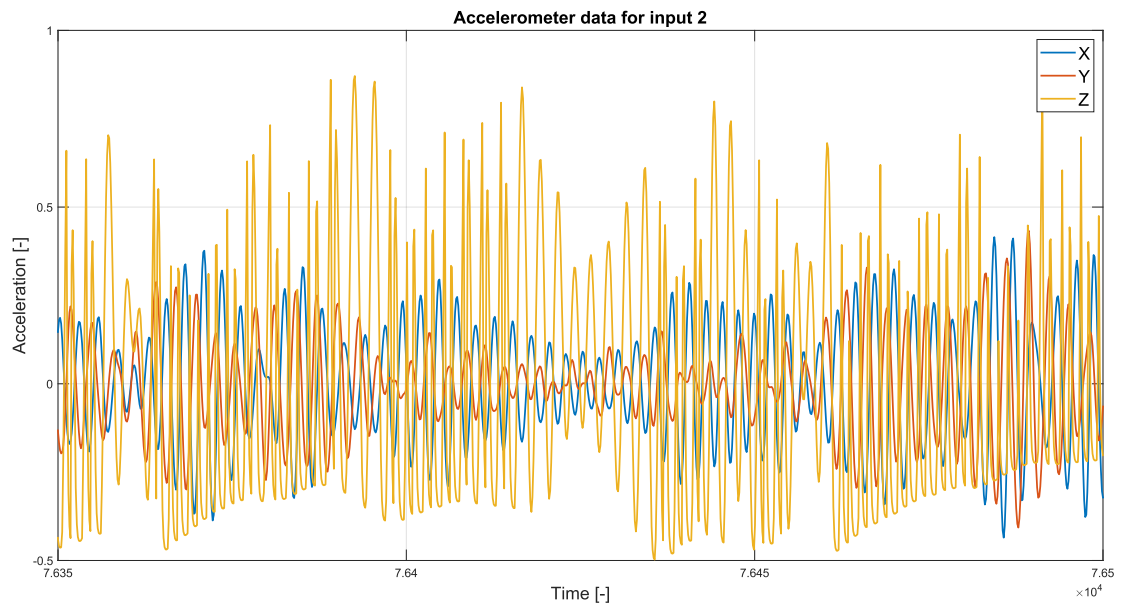


Figure 9.9: Accelerometer signal, clipping is visible

9.1.2 Parametric identification

The `bclsfits` developed by R.A. de Callafon has been used to obtain a parametric description of the plant. The function requires the specification of the model order and of the weight, when used. The input channels from 1 to 6 and all the three output channels have been selected. The input channels 7 and 8 have not been selected because shaker 7 and 8 are used as disturbance input. This results in a 3×6 state space model. To assess the impact of the model order (state dimension of the fitted model) on the fitting accuracy, this has been varied in between 12 and 144. The state dimension must be equal to a multiple of the number of inputs.

A choice should be made on the accuracy of the obtained model at different frequencies. The control bandwidth consist of the range in between $0.1f_c$ and $0.5f_c$, so in principle it should be sufficient to obtain an accurate model in this frequency range. However the closed loop system could present instability if an unknown disturbance is amplified at f_c . For this reason it has been decided to include also the f_c resonance peak in the parametric model.

Two performance criteria have been used to assess the quality of the parametric modelling, one quantitative and one qualitative:

- evaluation of the peak value of the dominant singular value of the difference between the measured FRF and the fitted n-th order parametric model;
- visual inspection of the Bode plot of the parametric fitting over the FRF.

Figure 9.13 shows how the maximum value of the largest singular value of the difference model varies with increasing model order. Figure 9.12 shows the largest singular value of the difference model at different frequencies and model order (the filled circles of Figure 9.12 are the maximum values of the singular value across frequency and hence are equal to the points in Figure 9.13).

If we look at the maximum of the singular value (Figure 9.13) we can see that the error decreases until model order 76, but this does not mean that the error decreases equally at all frequencies, since Figure 9.13 shows only the maximum value of the largest singular value across all frequencies. At low frequencies the singular value keeps decreasing until order 120 (see again Figure 9.12). Looking

9.1. System identification

at Figure 9.10 and Figure 9.11, we see that further increasing the order past 120, overfitting of the model appears. An order equal to 120 is necessary to accurately represent the system dynamics. Clearly, an equal order controller would be impossible to be implemented on satellite hardware. For this reason, it was decided to reduce the order of the plant using the balancing truncation method. As a matter of fact, it has been found that fitting an accurate model and then reducing it with the balancing truncation method provides better results than trying to directly fit a low order model (see Figure 9.17 and Figure 9.18.).

For what concerns weighting instead, it did not result in an improvement of the fitting. This can be attributed to the fact that to obtain a sharp bandpass weighting a high order is required for the weight. At the same time, the model filtered with a weight of high order needs more parameters to be described, which goes in contrast to the necessity of obtaining a low order model. For this reason, weighting was not used for the model fitting.

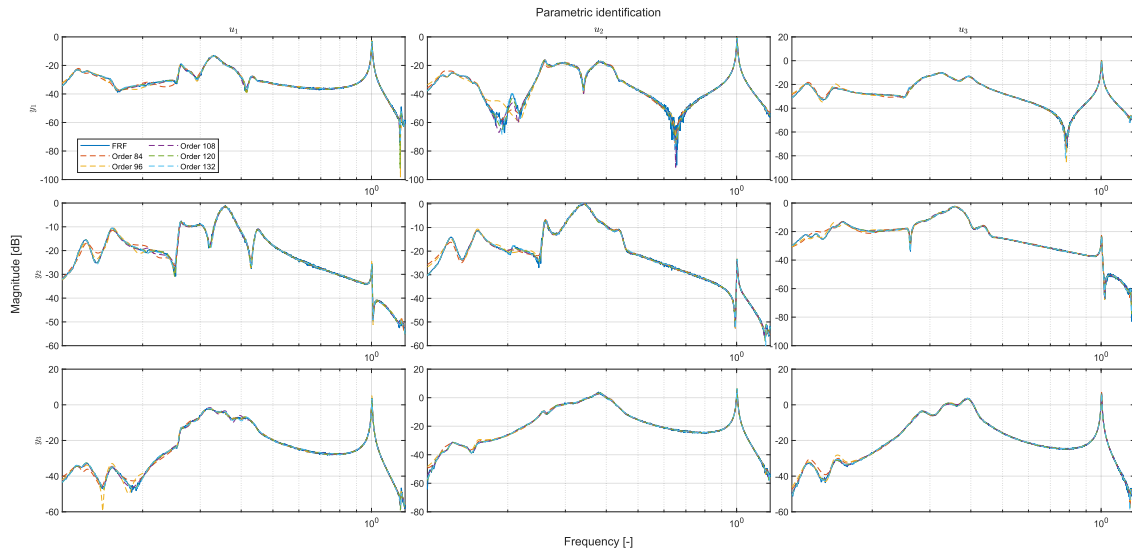


Figure 9.10: Bode magnitude plot of FRF and of fitted state space models, inputs 1 to 3

The balanced truncation method has been used to reduce the order of the model. Figure 9.17 shows the maximum of the largest singular value of the difference between the FRF and full fitted model for different model orders, and the maximum of the largest singular value of the difference between the FRF and model reduced starting from the fitted model of order 120 for different model order. Figure 9.16 instead shows the largest singular value of the difference between the FRF and model reduced starting from the fitted model of order 120 for different

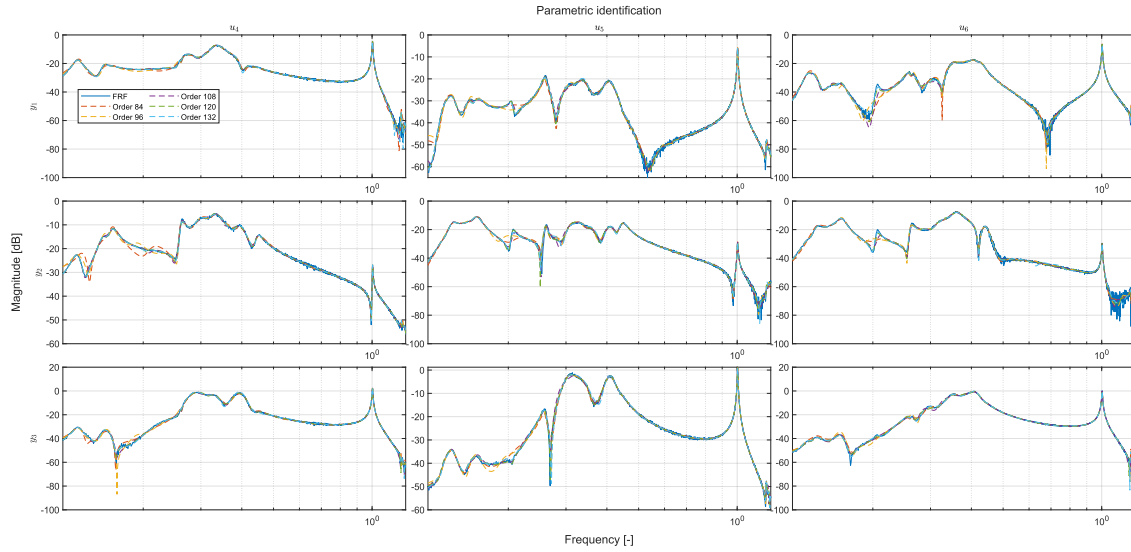


Figure 9.11: Bode magnitude plot of FRF and of fitted state space models, inputs 4 to 6

model order and for different frequencies.

Figure 9.14 and Figure 9.15 show the Bode magnitude plot of the reduced model at different orders. An order equal to 72 is sufficient to characterise the bread-board dynamics. To compare the results of model order reduction from 120 to 72 the case in which a model of order 72 is fitted directly from the FRF, the two Bode magnitude plot are shown in Figure 9.18. The error of the reduced model at low frequencies is lower than the fitted model for the same order. At higher frequencies instead the error is comparable. This comparison corroborates the choice of performing an accurate high-order fit from the FRF and then reducing it to a lower order.

9.1. System identification

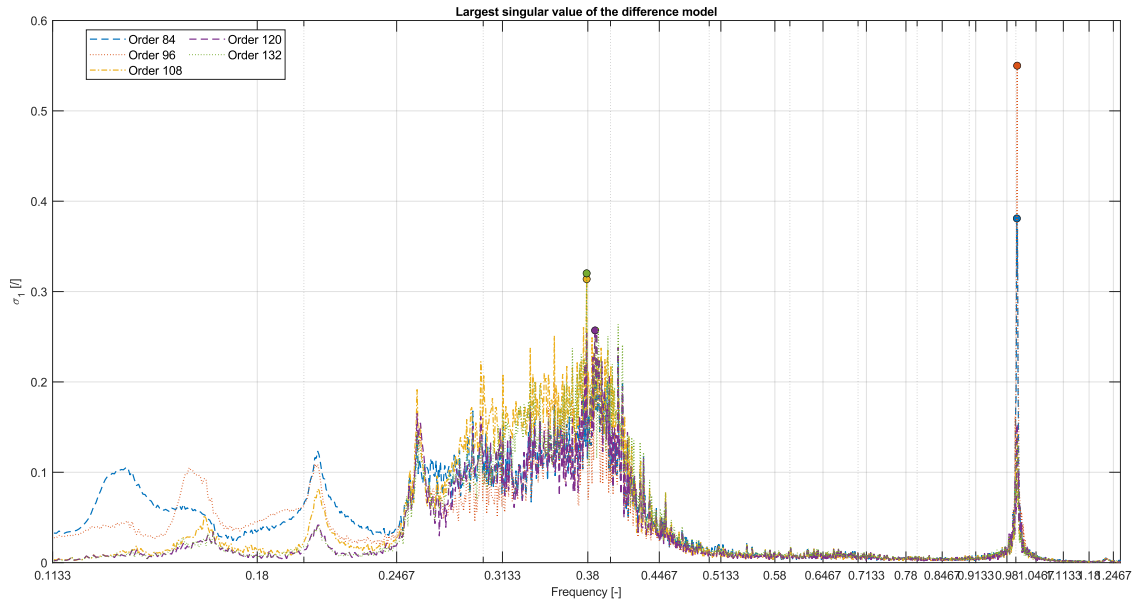


Figure 9.12: Bode magnitude plot of the largest singular value of the difference between FRF and fitted state space model at varying frequencies and order. The maximum of the singular value is shown with filled circles

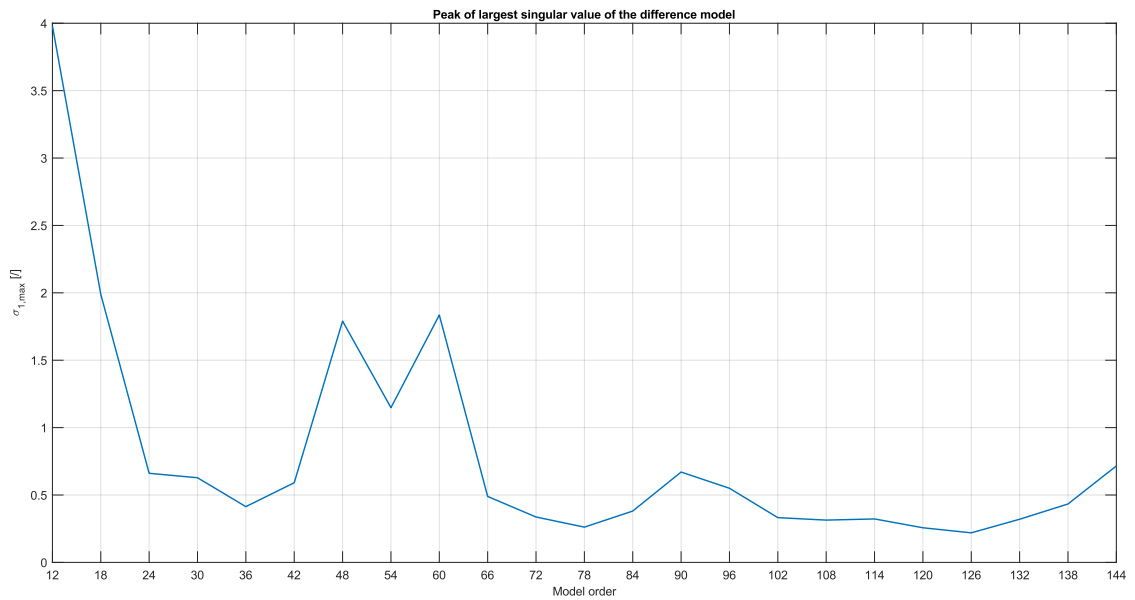


Figure 9.13: Bode magnitude plot of the maximum across frequencies of the largest singular value of the difference between FRF and fitted state space model at varying frequencies and order n .

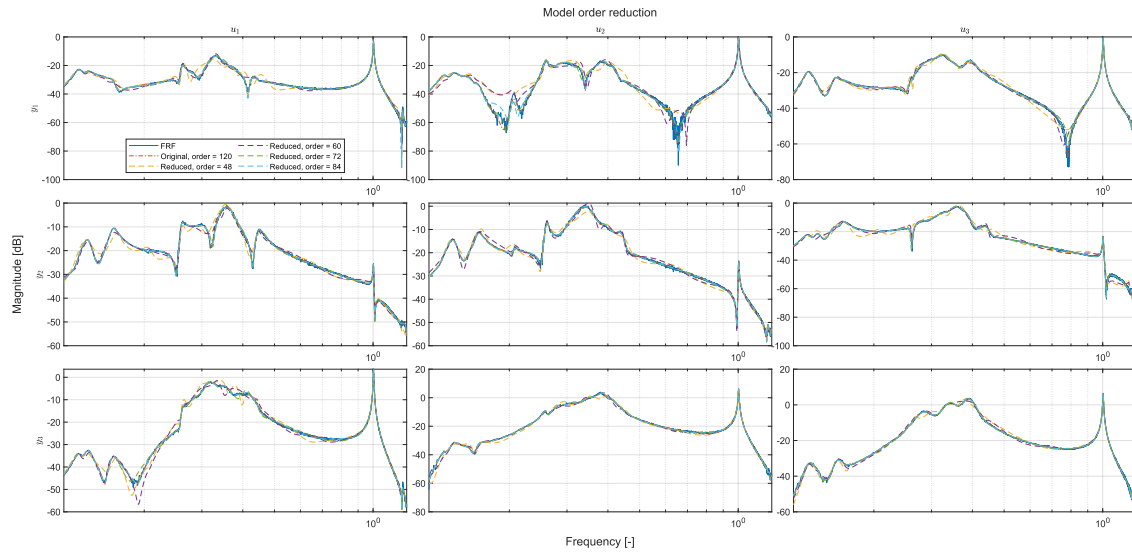


Figure 9.14: Bode magnitude plot of FRF, fitted state space model and reduced models, inputs 1 to 3

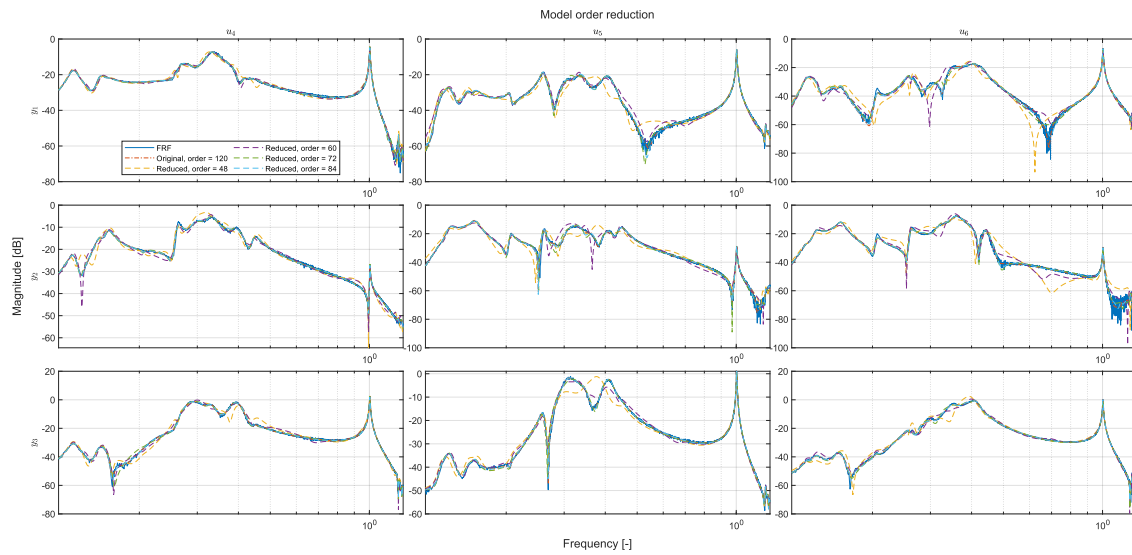


Figure 9.15: Bode magnitude plot of FRF, fitted state space model and reduced models, inputs 4 to 6

9.1. System identification

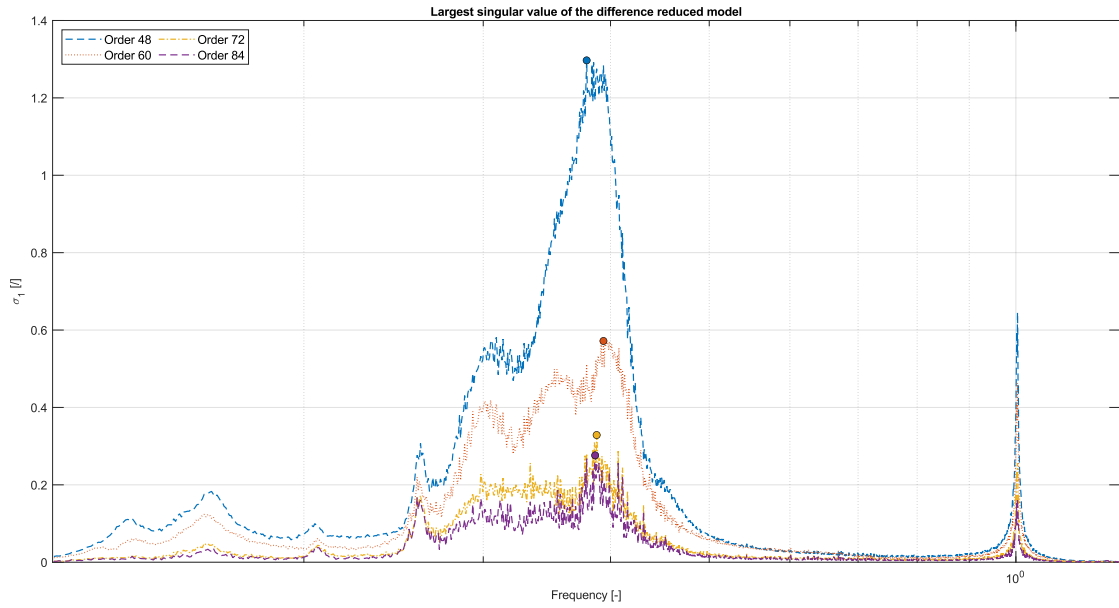


Figure 9.16: Bode magnitude plot of the largest singular value of the difference between FRF and reduced state space model at varying frequencies and order. The maximum in the singular value is shown with filled circles

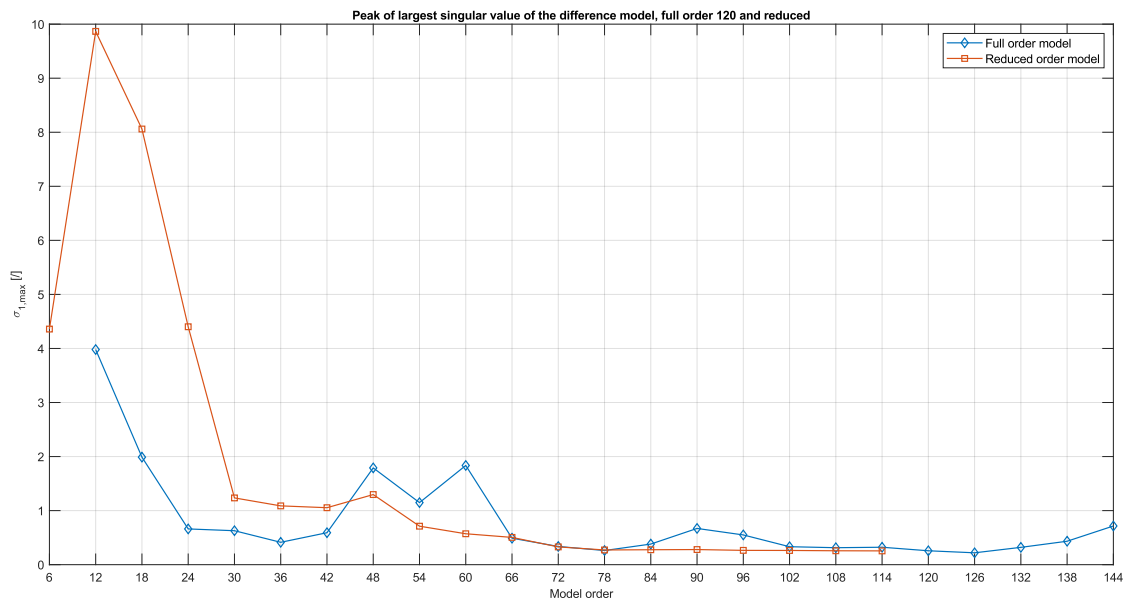


Figure 9.17: Bode magnitude plot of the maximum across frequencies of the largest singular value of the difference between FRF and the fitted state space model and of the difference between FRF and the reduced model at varying frequencies and order n .

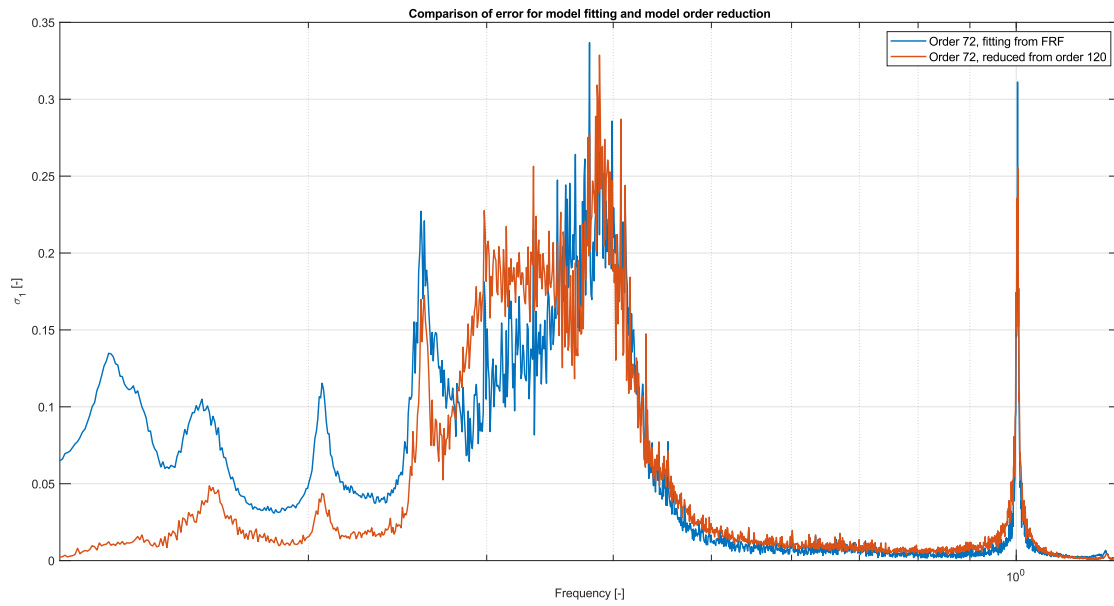


Figure 9.18: Bode magnitude plot of the largest singular value of the difference between FRF and the fitted state space model and of the difference between FRF and the reduced model at varying frequencies and equal order

9.1.3 Uncertainty modelling

The `ucover` MATLAB function has been used to define an uncertain plant model. The parametric system identification method has been used to obtain an accurate plant of order 120. The model has been reduced using balanced truncation method to order 72. The state space model of order 72 constitutes the nominal plant P_{nom} . The plant of order 120 is the plant P_{array} to be covered by the uncertain model. The difference between the two models was artificially amplified outside the identification frequency range to impose a greater uncertainty. The order of the weight W_1 has been varied to fit the optimal value of $W_{1,opt}$, W_2 is set to identity. The result of the fitting is shown Figure 9.19. A weight of order 19 has been chosen as a compromise between fitting accuracy and model order. The Bode magnitude response of realisations of this plant are shown in Figure 9.20 and Figure 9.21, together with the response of the nominal plant (plant of order 72) and the full order plant (order 120). The uncertainty in plant response at high frequency is then taken into account during controller synthesis by imposing a roll-off of the controller response.

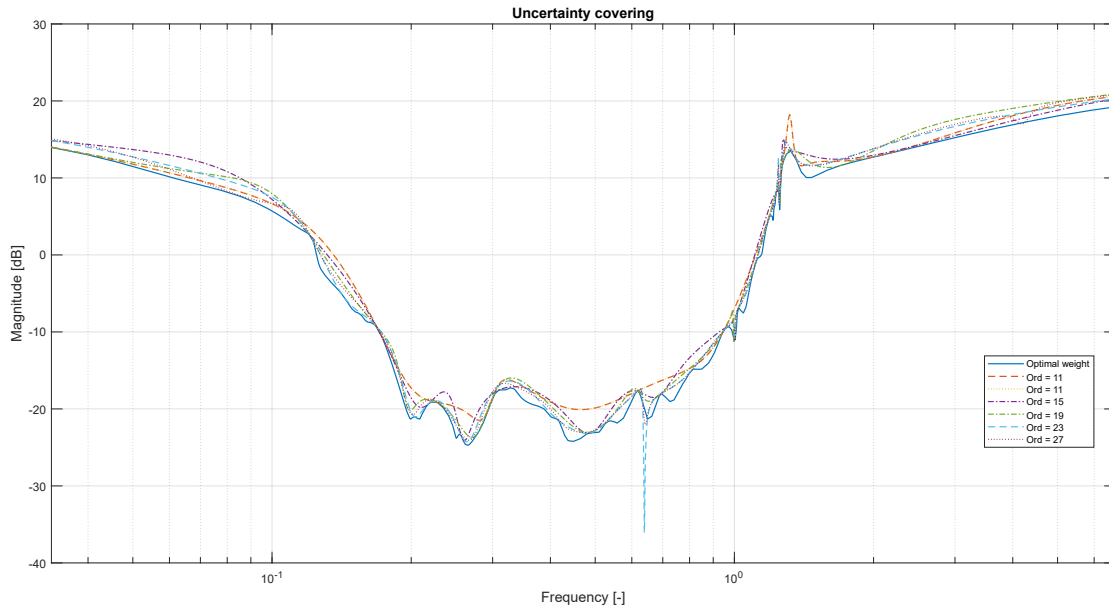


Figure 9.19: Bode magnitude plot of the optimal weight for uncertainty modelling and of weights of different order

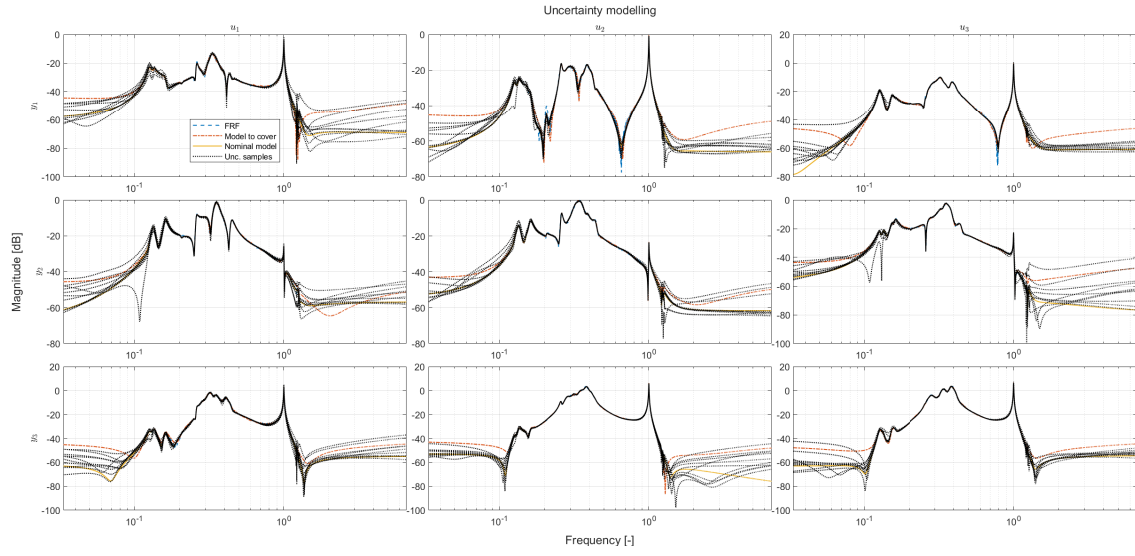


Figure 9.20: Bode magnitude plot of the nominal model, model to cover and samples of uncertain model, inputs 1 to 3

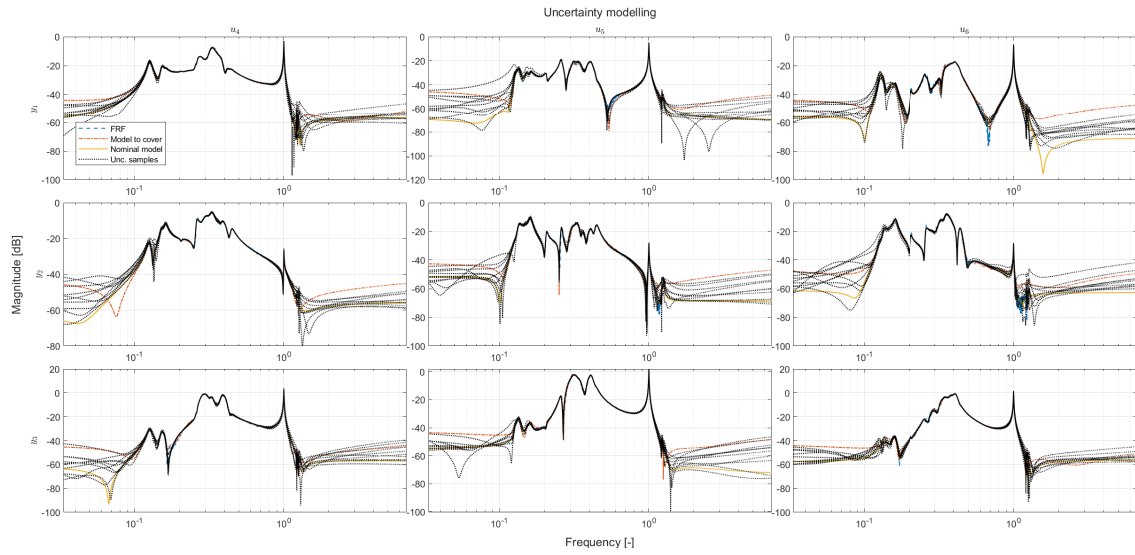


Figure 9.21: Bode magnitude plot of the nominal model, model to cover and samples of uncertain model, inputs 4 to 6

9.1.4 Summary of system identification results

This section summarises the results of the system identification process.

As a first step, a non-parametric model of the plant has been obtained in the form of FRF. All the system inputs (1 to 8) and system outputs (1 to 3) were considered. The non-parametric identification process can successfully be used to characterise the plant dynamics in the frequency range $0.1f_c$ to $1.3f_c$, with high SNR. The FRF of the plant is characterised by many modes in the frequency range and in particular by a predominant resonant peak at $f = f_c$. The source of the resonant peak has been characterised and it has been identified as the accelerometer beam resonance. The peak is indeed not present when changing the beam length and accelerometer mounting. In the non-parametric identification step, the effect of the LPM polynomial order has been assessed. Based on previous results in the literature, order 2 and 4 were investigated, showing no significant difference.

The second step consisted of parametric identification, where a state space model of the plant was obtained by fitting of the FRF. For the initial fitting, the order of the plant was chosen to accurately characterise the plant dynamics. To evaluate the goodness of the fit, Bode magnitude plots of the overlapped FRF and state space model were used as well as Bode magnitude plot of the largest singular value of the difference between the two. A model of order 120 was obtained. The high number of parameters is a result of the complex plant dynamics, characterised by many modes. The number of parameters was decreased by performing model order reduction. The system states with lower energy were neglected, resulting in a state space model of order 72. It was shown that performing model order reduction after accurate parameter fitting results in a more accurate model than directly fitting a model of equal order.

As a final step, the model order reduction error and the poor knowledge of the plant dynamics outside the $0.1f_c - 1.3f_c$ range, were taken into account into the uncertain modelling. An uncertain state space plant was obtained using the `ucover` MATLAB function, which provides a conservative cover of the error between two reference models. The uncertain plant can be used to evaluate the controllers RS and RS.

9.2 Controllers synthesis and testing

Four types of controllers have been designed SISO LTI, MIMO LTI, SISO LPV and MIMO LPV. The stability and performance objectives are imposed through loop shaping, as described in Chapter 8. The obtained closed loop performance are reported in this section, together with the result of the controller testing. Section 9.2.1 and Section 9.2.2 presents the SISO and MIMO LTI control results while Section 9.2.3 and Section 9.2.4 the SISO and MIMO LPV control results. Finally, Section 9.2.5 summarises the obtained results.

9.2.1 SISO LTI control

For the SISO controller design, input actuator 2 has been chosen to control the output 3. From the FRF of input 7 (disturbance input), the two frequencies inside the control bandwidth that correspond to the largest input amplification have been chosen for the disturbance: $0.30f_c$ and $0.42f_c$. The procedure described in Section 8.4 has been used to synthesise LTI controller which achieves RS, NP or RP.

Table 9.1 reports the sensitivity weight matrix W_S and control sensitivity weight matrix W_{KS} for the two LTI controllers.

Table 9.2 reports the achieved performance in terms of:

- H_∞ norm γ of the (augmented) closed loop transfer function, which is a measure of NP;
- uncertainty set for which the RS is guaranteed;
- maximum value of the structured singular value μ for the (augmented) closed loop which is a measure of RP.

The Bode magnitude plot of the closed loop systems are shown in figures from Figure 9.22 to Figure 9.29.

The controllers have been tested in simulation using Simulink and on the real breadboard. In simulation, the full order plant is used in closed loop with the structured controller, to closely represent the real plant dynamics. An output disturbance is simulated with frequency equal to 0.30 or 0.42. The output is also affected by measurement noise.

9.2. Controllers synthesis and testing

Table 9.1: SISO LTI controller design performance weights parameters

Freq. [-]	Goal	W_S base level [dB]	W_S inverse notch depth [dB]	W_S damping factor [-]	W_{KS} DC gain [dB]	W_{KS} high freq. gain [dB]	W_{KS} rolloff freq. [-]
0.30	NP & RS	-6	26	0.002	-25.6169	20	1.33
	RP & RS	-6	22	0.002	-25.6169	20	1.33
0.42	NP & RS	-6	26	0.002	-25.6169	20	1.33
	RP & RS	-6	18	0.002	-25.6169	20	1.33

The performance of the controller in simulation and on the breadboard has been characterised using the reduction ratio ρ performance index. This is defined as:

$$\rho = 1 - \frac{y_{RMS,CL}}{y_{RMS,OL}} \quad (9.2)$$

where $y_{RMS,CL}$ is the RMS of the output in closed loop and $y_{RMS,OL}$ is the RMS of the output in open loop. The ratio between the closed loop and open loop dynamics is also reported in dB units. The results of the simulation and of the test on the breadboard are reported in Table 9.3. In general, there is correlation between the results obtained in simulation and the one on the breadboard, which indicate that the modelling performed through system identification is accurate. All the SISO LTI controllers effectively reduce the vibration of a factor 10 ($20dB$) or greater. Figures from Figure 9.30 to Figure 9.33 show the results for the tests on the breadboard.

Table 9.2: SISO LTI controller design performance metrics

Frequency	Goal	Full order		Reduced order		Structured	
0.30	NP & RS	Ord.	90	Ord.	23	Ord.	6
		γ	0.8009	γ	0.9843	γ	0.8613
		RS	2.45 Δ	RS	2.45 Δ	RS	1.86 Δ
	RP & RS	Ord.	126	Ord.	31	Ord.	9
		μ	0.9513	μ	0.9628	μ	0.9911
		RP	2.32 Δ	RP	2.32 Δ	RP	1.80 Δ
0.42	NP & RS	Ord.	90	Ord.	15	Ord.	7
		γ	0.8018	γ	0.8834	γ	0.8535
		RS	2.45 Δ	RS	2.45 Δ	RS	2.38 Δ
	RP & RS	Ord.	130	Ord.	19	Ord.	10
		μ	0.9580	μ	0.9901	μ	0.9956
		RP	2.32 Δ	RP	2.32 Δ	RP	2.03 Δ

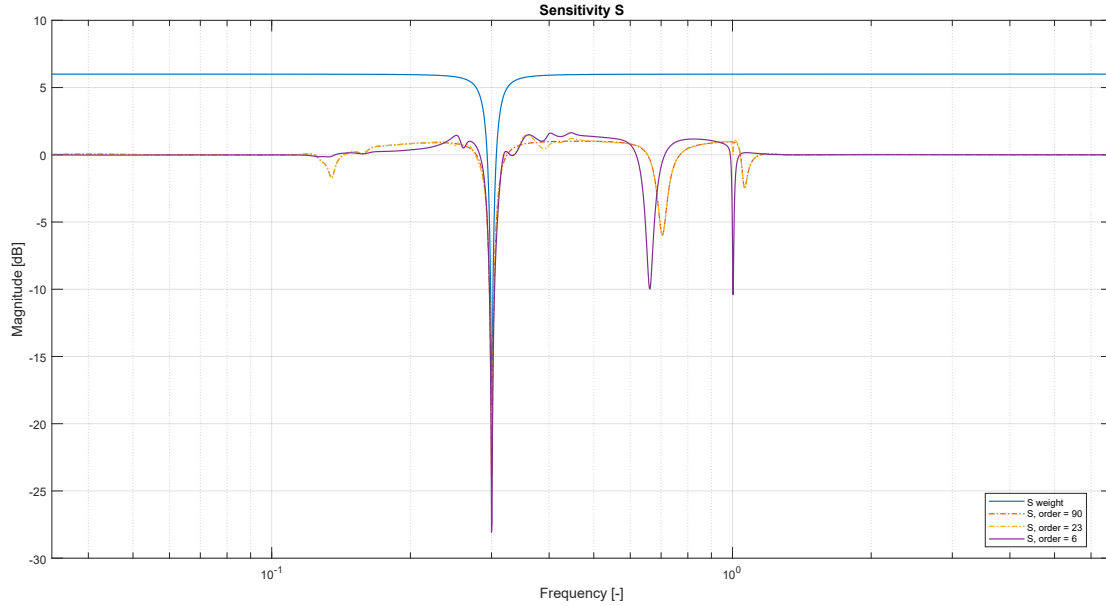


Figure 9.22: Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for NP and RS at 0.30 frequency

9.2. Controllers synthesis and testing

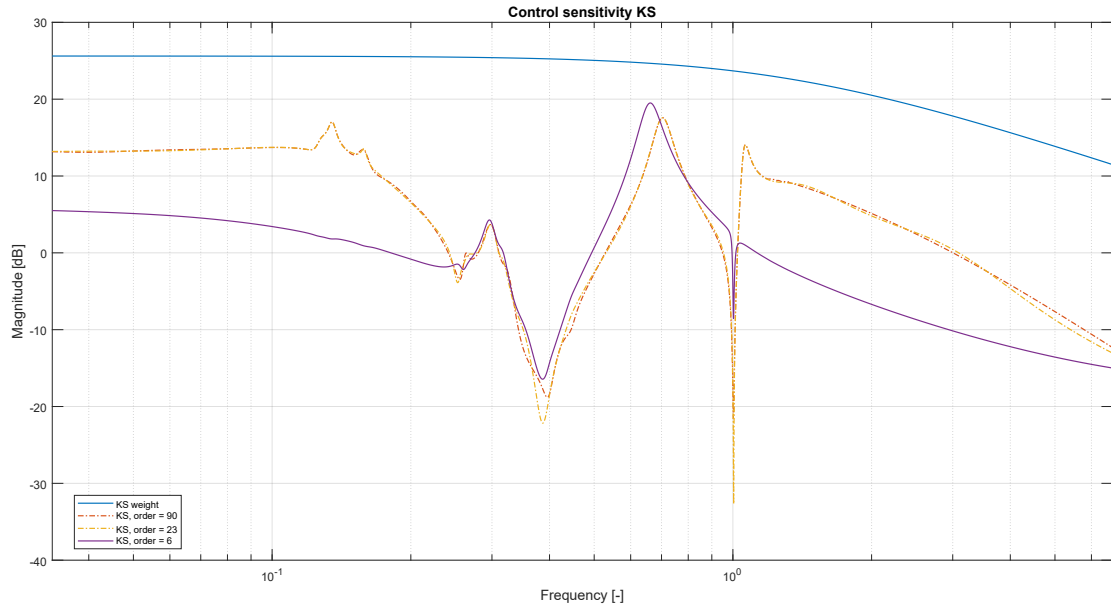


Figure 9.23: Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for NP and RS at 0.30 frequency

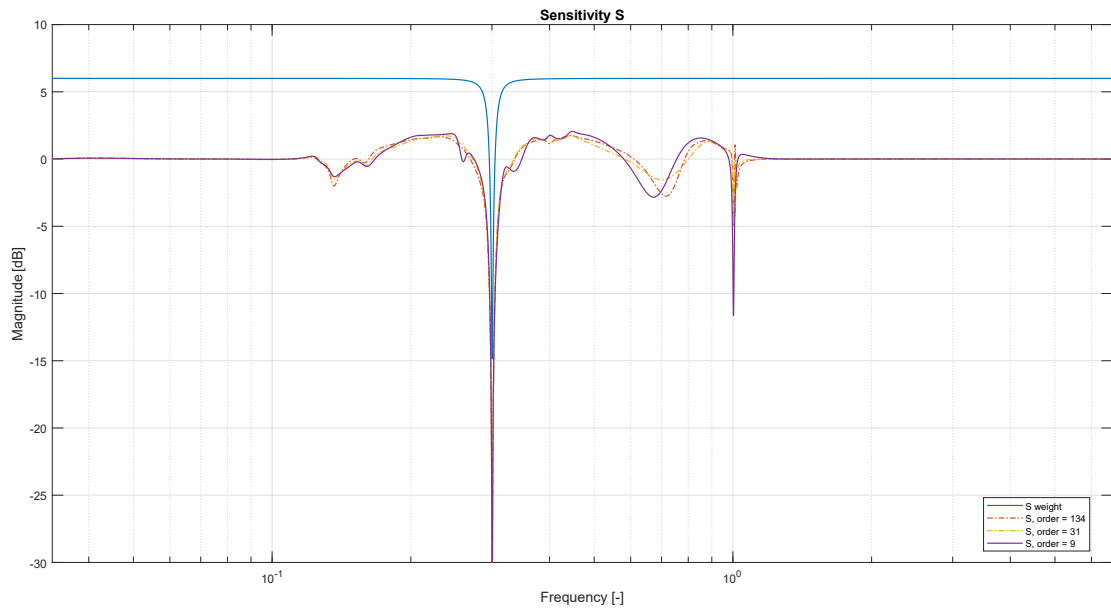


Figure 9.24: Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for RP and RS at 0.30 frequency

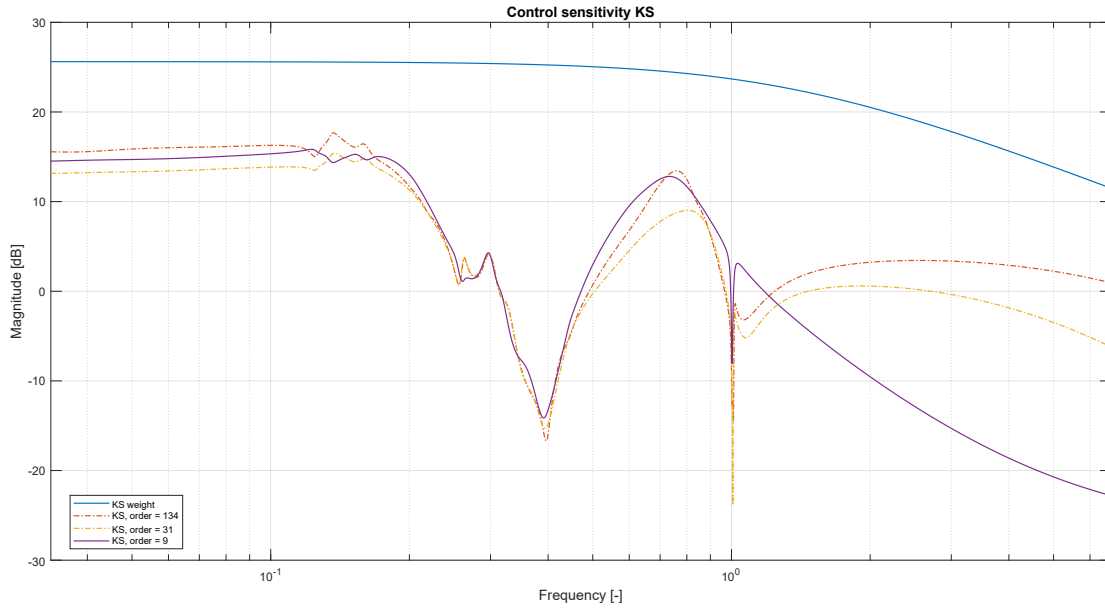


Figure 9.25: Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for RP and RS at 0.30 frequency

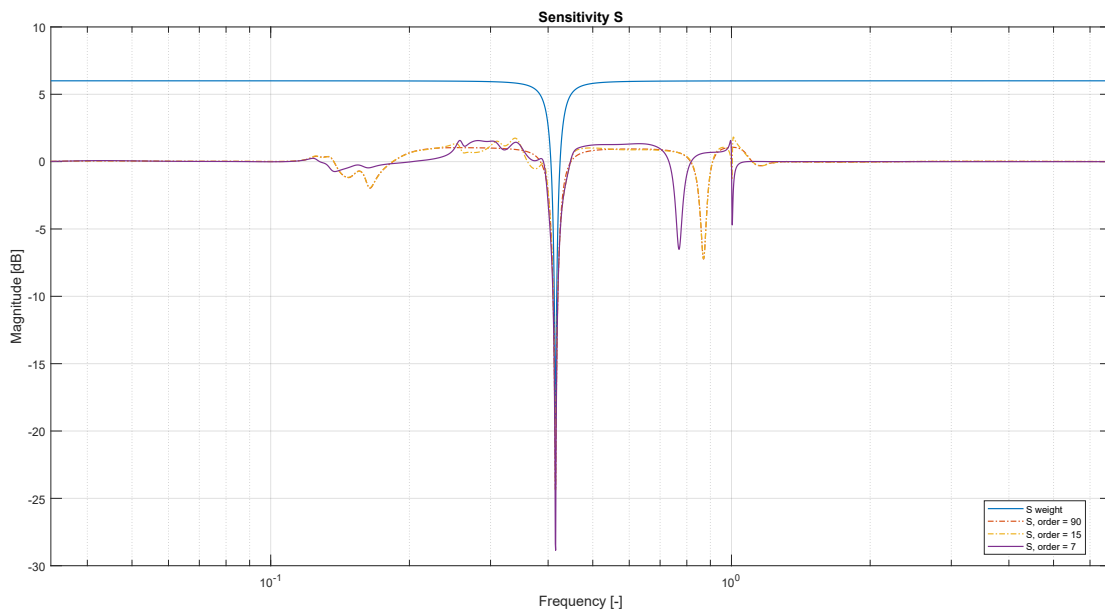


Figure 9.26: Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for NP and RS at 0.42 frequency

9.2. Controllers synthesis and testing

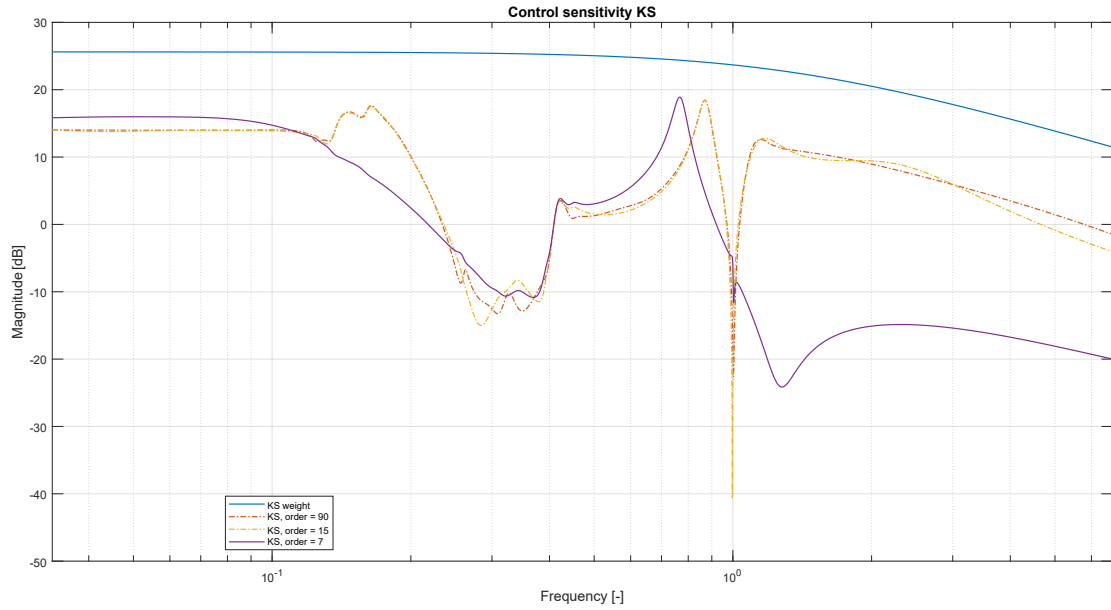


Figure 9.27: Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for NP and RS at 0.42 frequency

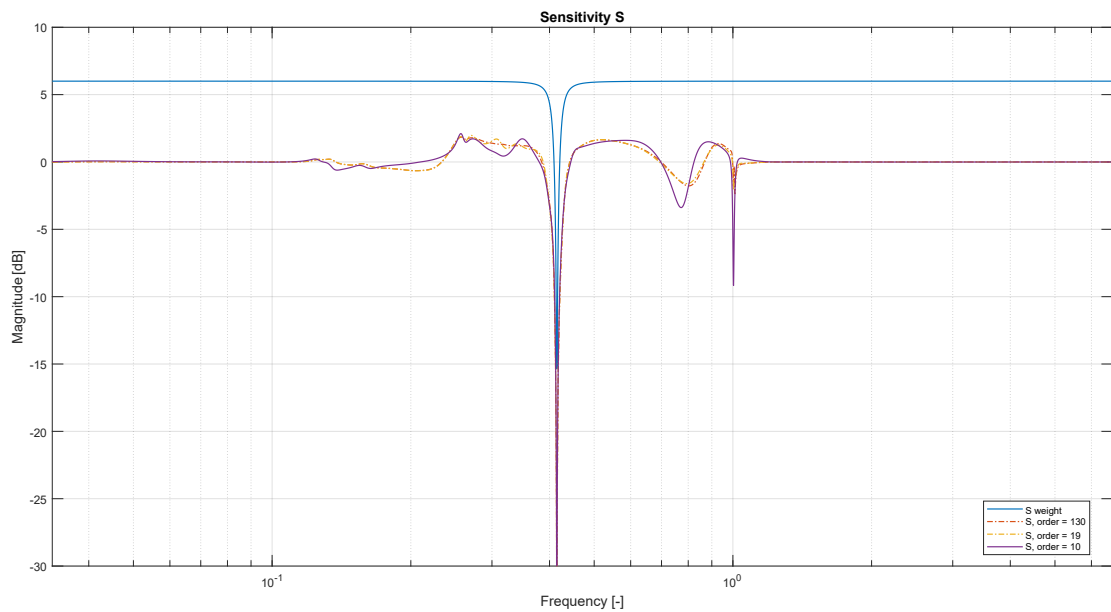


Figure 9.28: Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for RP and RS at 0.42 frequency

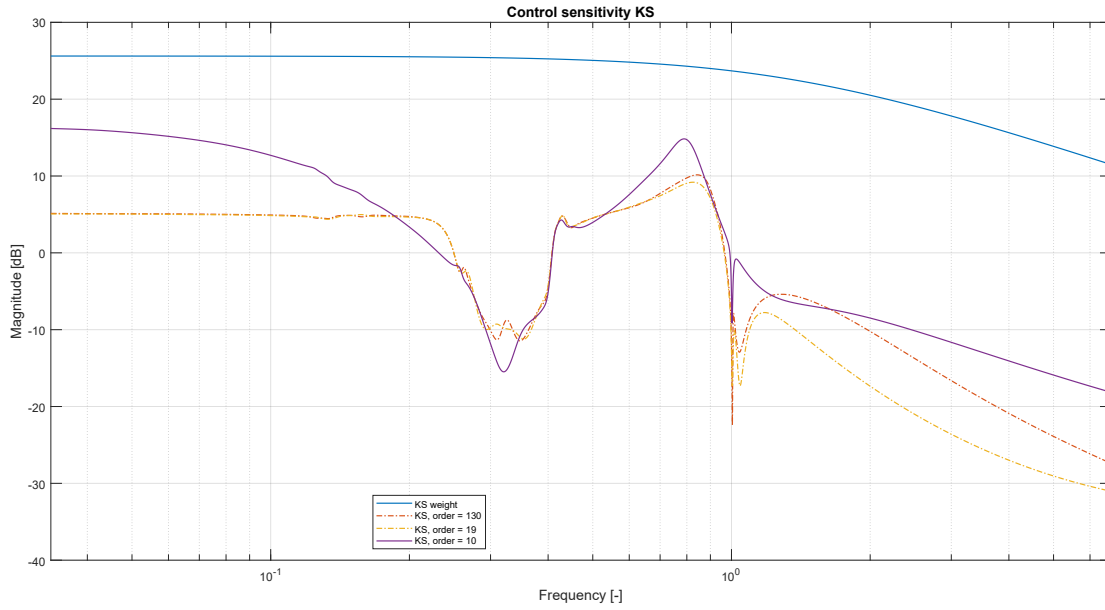


Figure 9.29: Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. SISO LTI controller for RP and RS at 0.42 frequency

Table 9.3: Results of simulation and test on breadboard of the SISO LTI controllers

Freq.	Goal	Performance metric	Test in simulation	Test on the breadboard
0.30	NP & RS	OL RMS [-]	0.0234	0.0189
		CL RMS [-]	0.0014	0.0015
		ρ [-]	94.11	92.31
		CL/OL RMS [dB]	-24.60	-22.24
	RP & RS	OL RMS [-]	0.0234	0.0188
		CL RMS [-]	0.0012	0.0012
		ρ [-]	94.93	93.48
		CL/OL RMS [dB]	-25.91	-23.63
0.42	NP & RS	OL RMS [-]	0.0588	0.0569
		CL RMS [-]	0.0021	0.0015
		ρ [-]	96.37	97.30
		CL/OL RMS [dB]	-28.80	-31.44
	RP & RS	OL RMS [-]	0.0588	0.0547
		CL RMS [-]	0.0018	0.0014
		ρ [-]	96.97	97.36
		CL/OL RMS [dB]	-30.37	-31.65

9.2. Controllers synthesis and testing

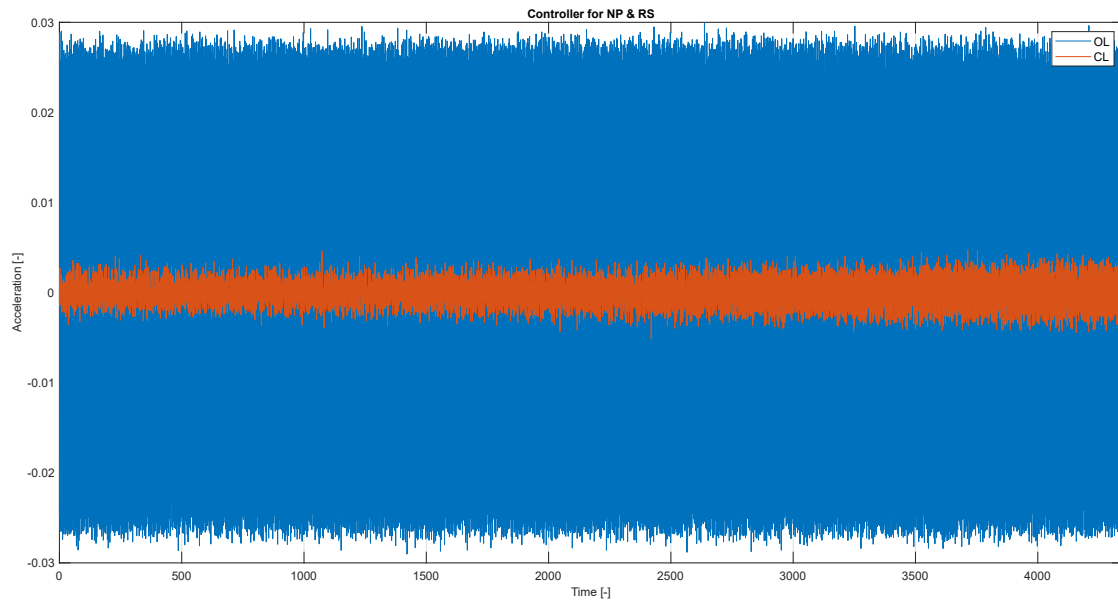


Figure 9.30: Result of test of controller SISO LTI for NP and RS on the breadboard, disturbance frequency equal to 0.30

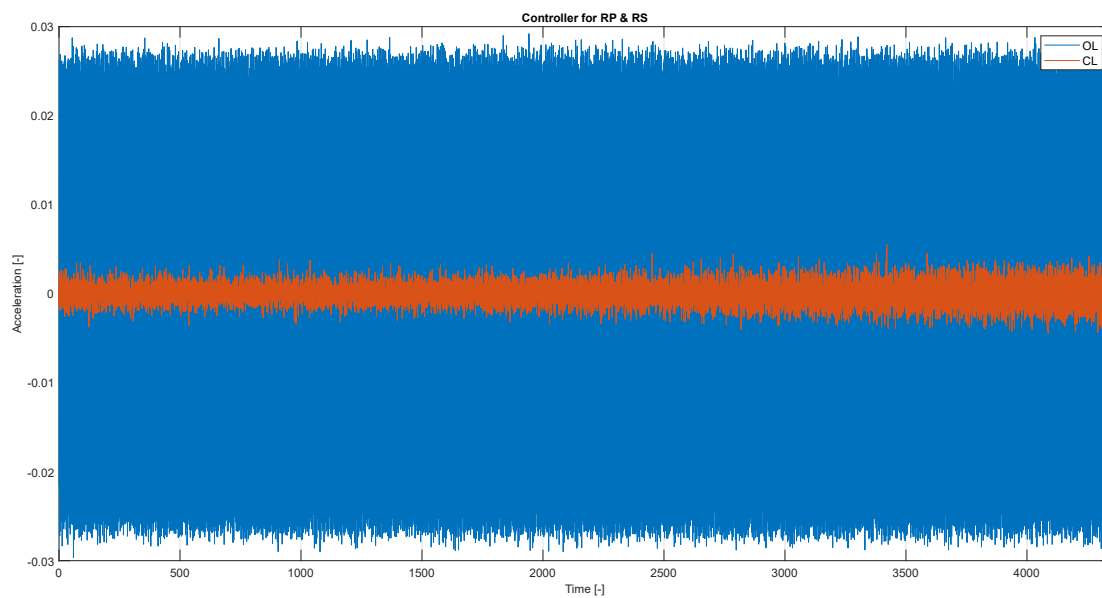


Figure 9.31: Result of test of controller SISO LTI for RP and RS on the breadboard, disturbance frequency equal to 0.30

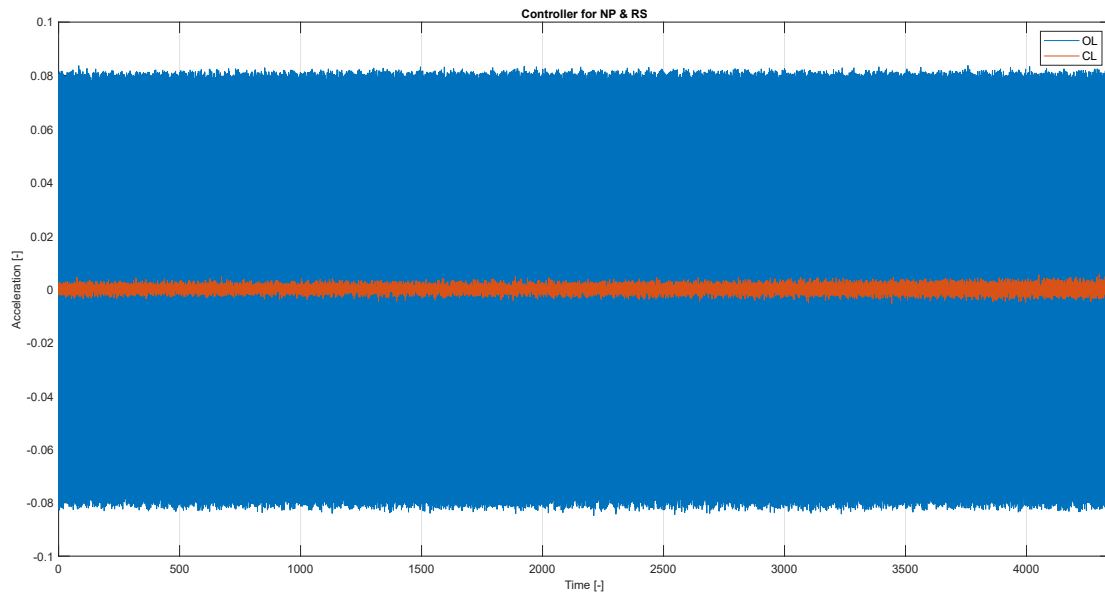


Figure 9.32: Result of test of controller SISO LTI for NP and RS on the breadboard, disturbance frequency equal to 0.42

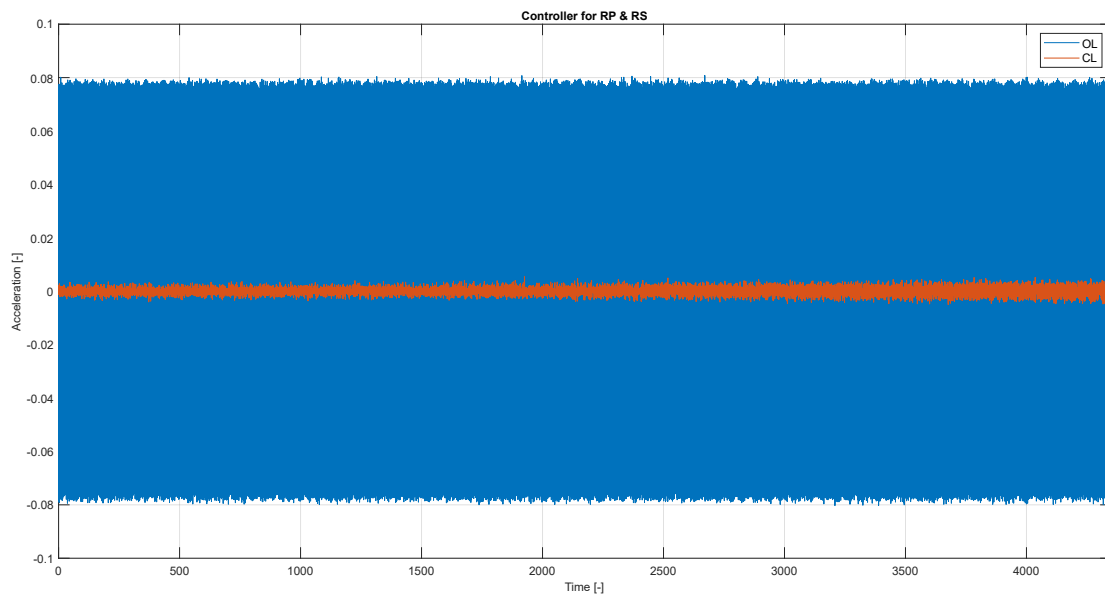


Figure 9.33: Result of test of controller SISO LTI for RP and RS on the breadboard, disturbance frequency equal to 0.42

9.2.2 MIMO LTI control

The LTI MIMO controller uses shaker from 1 to 6 to control all three acceleration components. The shaping weights are diagonal matrices which shape the diagonal entries of the output sensitivity and output control sensitivity matrices S and KS . The FRF of input 7 (disturbance shaker) to output 1 to 3 is used to identify the frequencies at which the FRF peaks, which constitute worst case scenarios. The 0.30 and 0.35 frequencies are selected.

The parameters of the shaping weights are reported in Table 9.4, while the closed loop performance metrics are reported in Table 9.5. Figures from Figure 9.34 to Figure 9.41 show the sensitivity S and control sensitivity KS and weighting function Bode magnitude plots.

Table 9.4: MIMO LTI controller design performance weights parameters

Freq. [-]	Goal	W_S base level [dB]	W_S inverse notch depth [dB]	W_S damping factor [-]	W_{KS} DC gain [dB]	W_{KS} high freq. gain [dB]	W_{KS} rolloff freq. [-]
0.30	NP & RS	-6	14	0.005	-21.1799	20	1.33
	RP & RS	-6	13	0.005	-21.1799	20	1.33
0.35	NP & RS	-6	18	0.005	-21.1799	20	1.33
	RP & RS	-6	12	0.005	-21.1799	20	1.33

The two pairs of controllers have been tested both in simulation and on the breadboard. Table 9.6 resumes the result of both sets of tests. In simulation it is made the conservative assumption that the disturbance acts on the output with the same amplitude for all the three acceleration components. The amplitude is set equal to the largest between the three acceleration components when disturbance input shaker 7 is active. In reality the disturbance input shaker 7

Table 9.5: MIMO LTI controller design performance metrics

Frequency	Goal	Full order		Reduced order		Structured	
0.30	NP & RS	Ord.	135	Ord.	37	Ord.	7
		γ	0.7665	γ	0.8806	γ	0.9808
		RS	1.85 Δ	RS	1.74 Δ	RS	1.61 Δ
	RP & RS	Ord.	307	Ord.	41	Ord.	14
		μ	0.9753	μ	0.9939	μ	0.9931
		RP	3.16 Δ	RP	3.17 Δ	RP	3.20 Δ
0.35	NP & RS	Ord.	135	Ord.	47	Ord.	7
		γ	0.8034	γ	0.8860	γ	0.9861
		RS	1.18 Δ	RS	1.29 Δ	RS	1.11 Δ
	RP & RS	Ord.	235	Ord.	56	Ord.	16
		μ	0.9502	μ	0.9931	μ	0.9919
		RP	3.15 Δ	RP	3.15 Δ	RP	3.00 Δ

results in three different values of acceleration, hence the different open loop acceleration values in Table 9.6. Figures from Figure 9.42 to Figure 9.45 show the results of the tests on the breadboard, while the plots of the results of the simulation are omitted for brevity. Note that the axis vertical scale is different for the three axes. All the controllers are able to bring the acceleration amplitude almost down to sensor noise level. All the controller reduce the level of disturbance up to a factor 5 ($14dB$) on the axis with greater open loop disturbance.

9.2. Controllers synthesis and testing

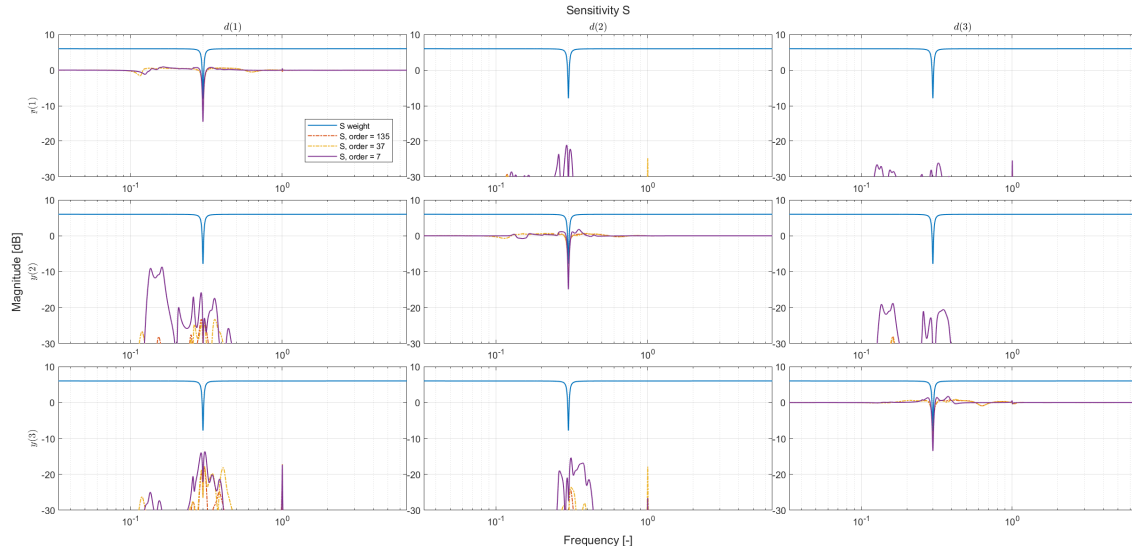


Figure 9.34: Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for NP and RS at 0.30 frequency

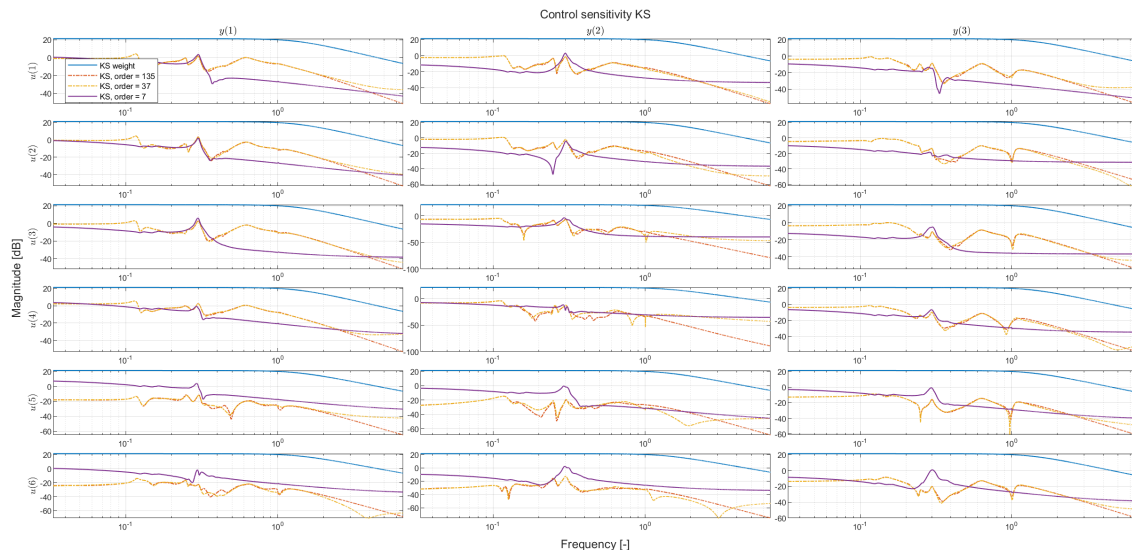


Figure 9.35: Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for NP and RS at 0.30 frequency

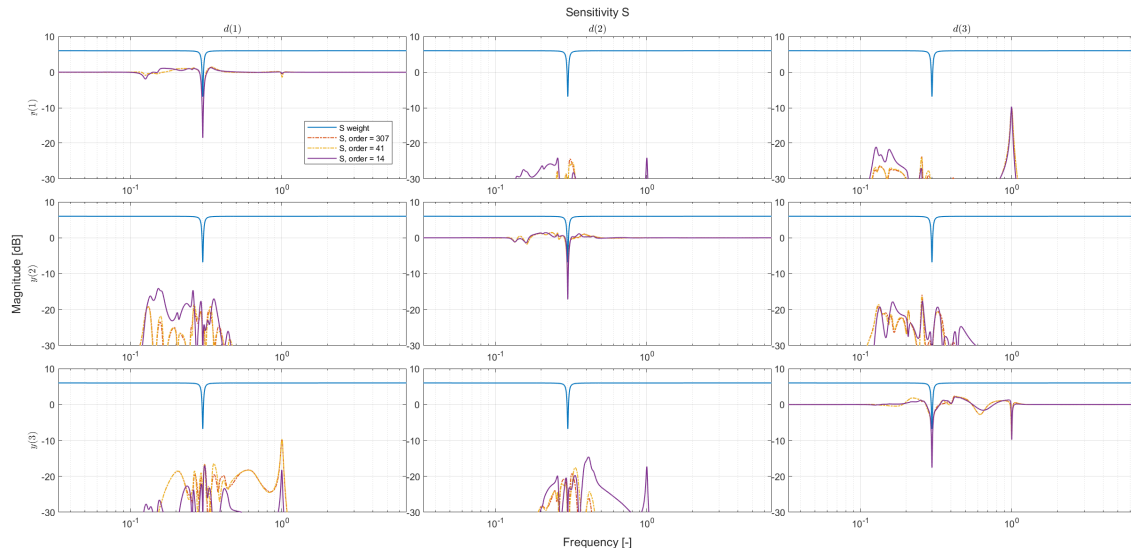


Figure 9.36: Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for RP and RS at 0.30 frequency

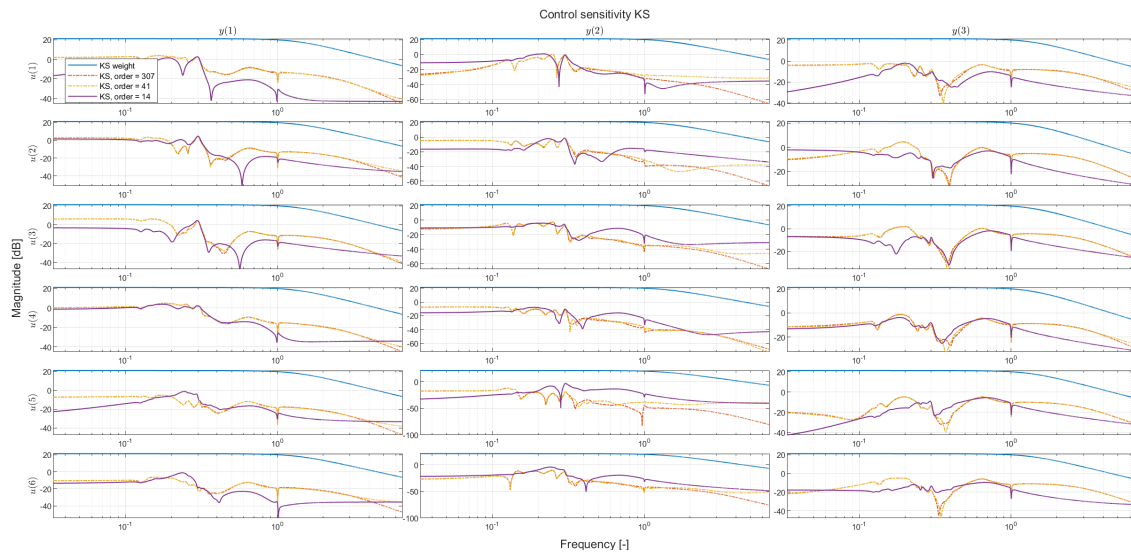


Figure 9.37: Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for RP and RS at 0.30 frequency

9.2. Controllers synthesis and testing

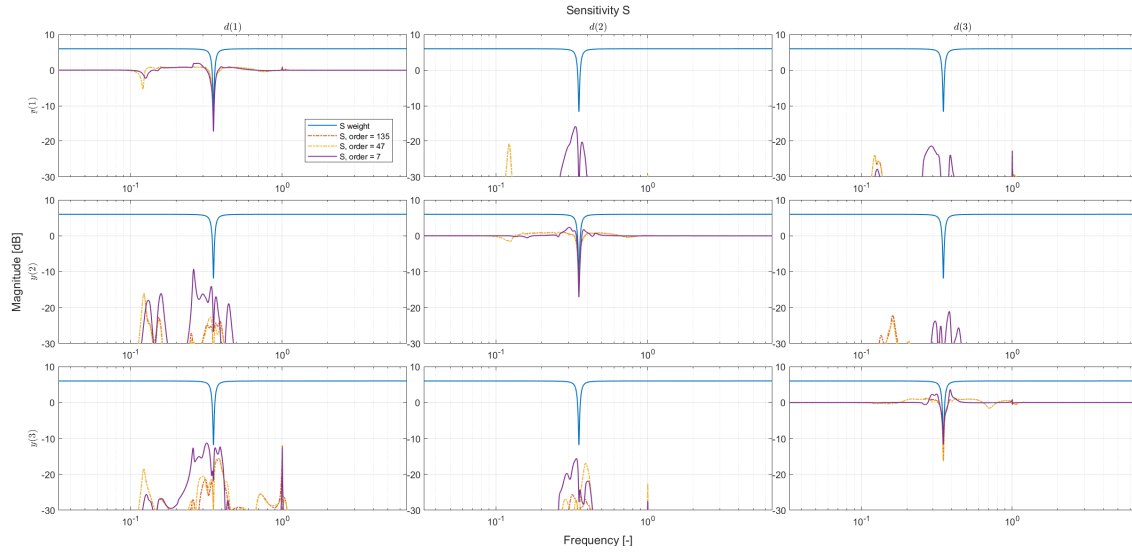


Figure 9.38: Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for NP and RS at 0.35 frequency

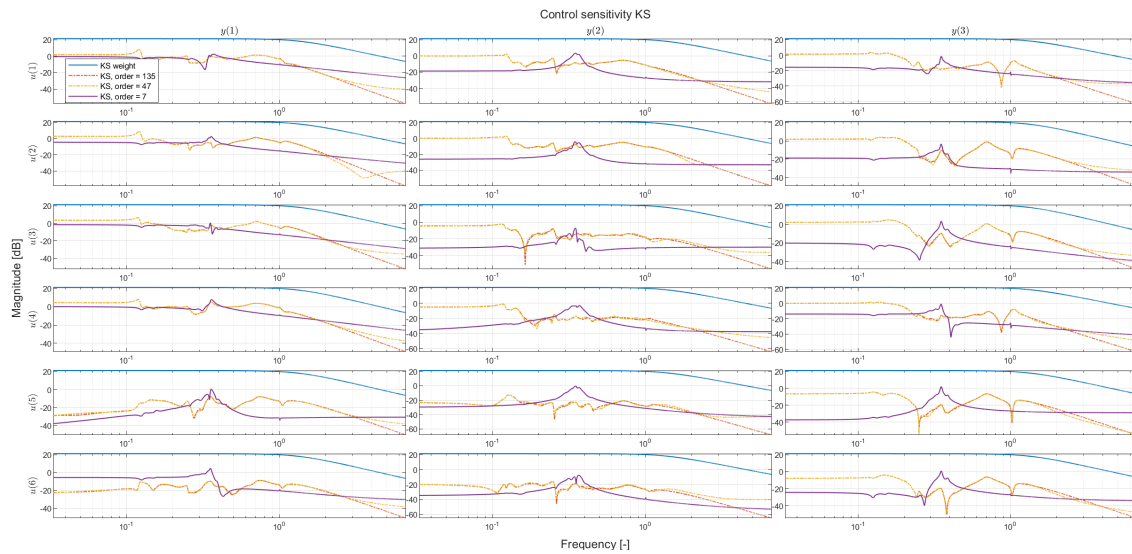


Figure 9.39: Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for NP and RS at 0.35 frequency

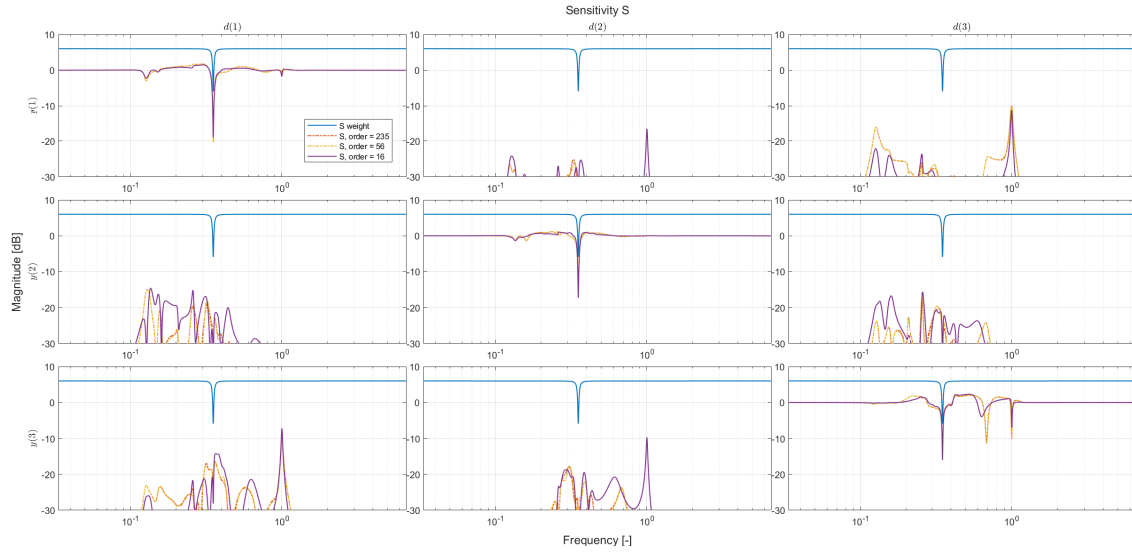


Figure 9.40: Bode magnitude plot of the sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for RP and RS at 0.35 frequency

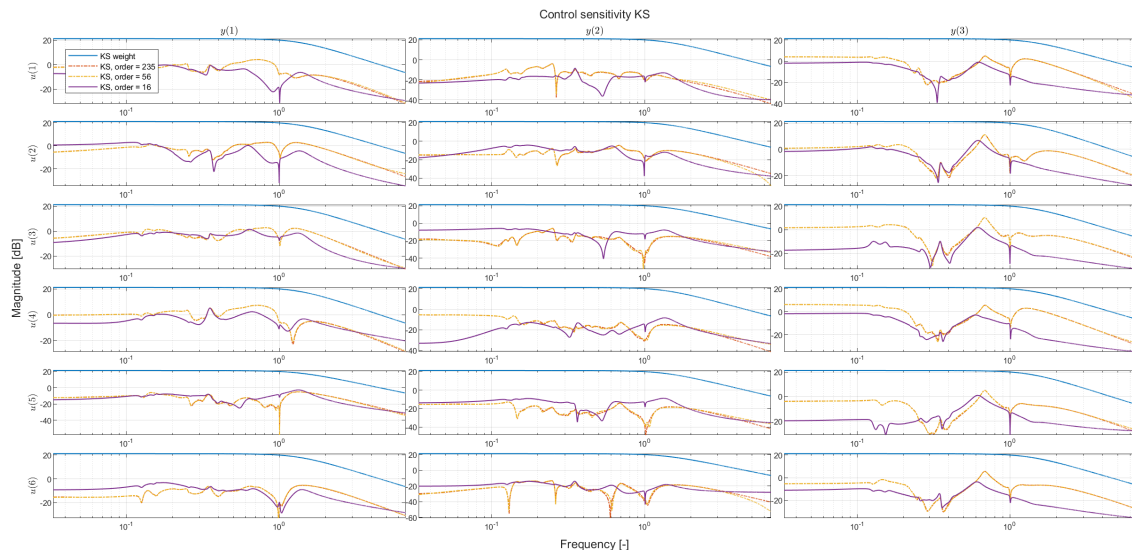


Figure 9.41: Bode magnitude plot of the control sensitivity function for the full order controller, reduced order controller and structured controller. MIMO LTI controller for RP and RS at 0.35 frequency

9.2. Controllers synthesis and testing

Table 9.6: Results of simulation and test on breadboard for the MIMO LTI controllers

Freq. [-]	Goal	Perf. metric	Test in simulation			Test on the breadboard		
			x	y	z	x	y	z
0.30	NP & RS	OL RMS [-]	0.0234	0.0234	0.0234	0.0017	0.0064	0.0183
		CL RMS [-]	0.0053	0.0048	0.0040	0.0011	0.0015	0.0027
		ρ [-]	77.37	79.28	82.93	36.09	76.15	85.38
		CL/OL RMS [dB]	-12.91	-13.67	-15.35	-3.93	-12.48	-16.72
	RP & RS	OL RMS [-]	0.0234	0.0234	0.0234	0.0017	0.0066	0.0183
		CL RMS [-]	0.0029	0.0037	0.0025	0.0009	0.0013	0.0021
		ρ [-]	87.71	84.09	89.26	44.62	80.77	88.3
		CL/OL RMS [dB]	-18.21	-15.97	-19.38	-5.24	-14.32	-18.69

Table 9.6: (continued)

Freq. [-]	Goal	Perf. metric	Test in simulation			Test on the breadboard		
			x	y	z	x	y	z
0.35	NP & RS	OL RMS [-]	0.0162	0.0162	0.0162	0.0061	0.0103	0.0145
		CL RMS [-]	0.0026	0.0035	0.0056	0.0011	0.0022	0.0082
		ρ [-]	83.79	78.25	65.61	82.09	78.67	43.65
		CL/OL RMS [dB]	-15.80	-13.25	-9.27	-14.83	-13.46	-4.98
	RP & RS	OL RMS [-]	0.0162	0.0162	0.0162	0.0062	0.0103	0.0142
		CL RMS [-]	0.0023	0.0030	0.0025	0.0011	0.0019	0.0019
		ρ [-]	86.02	81.60	84.43	82.41	81.25	86.43
		CL/OL RMS [dB]	-17.09	-14.70	-16.15	-15.17	-14.48	-17.30

9.2. Controllers synthesis and testing

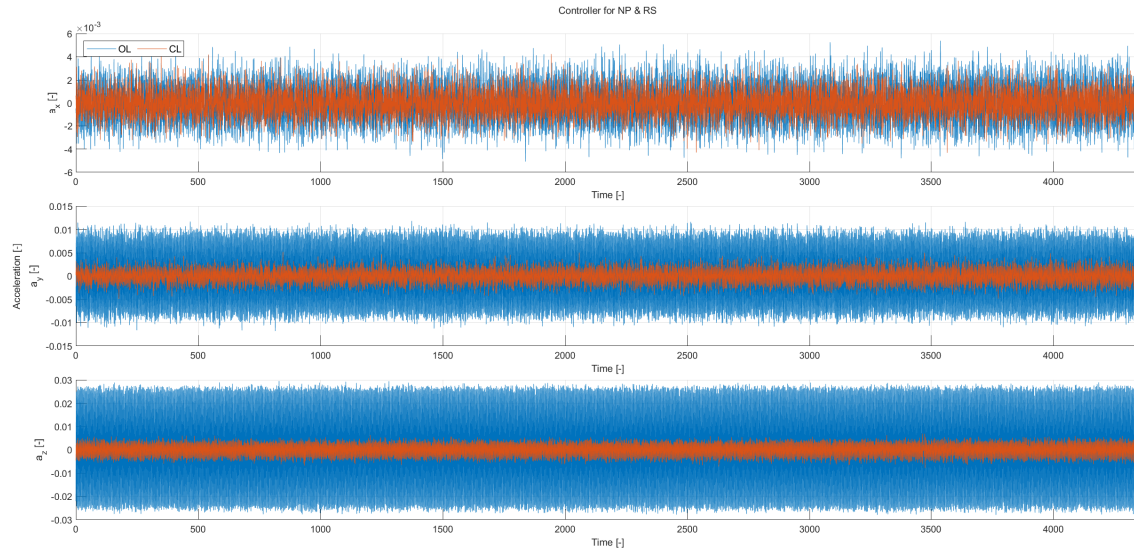


Figure 9.42: Result of test of controller MIMO LTI for NP and RS on the breadboard, disturbance frequency equal to 0.30

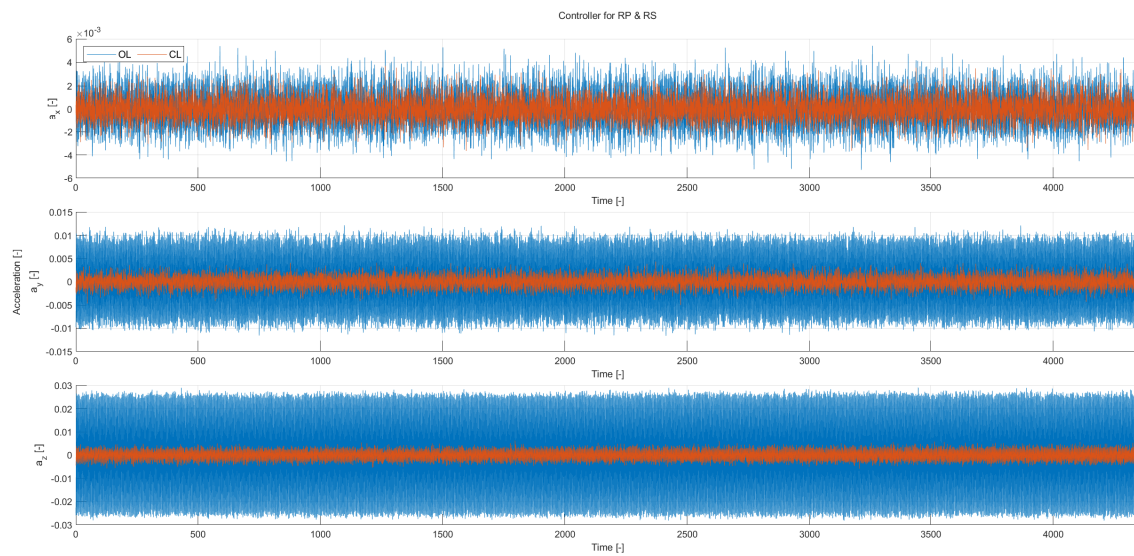


Figure 9.43: Result of test of controller MIMO LTI for RP and RS on the breadboard, disturbance frequency equal to 0.30

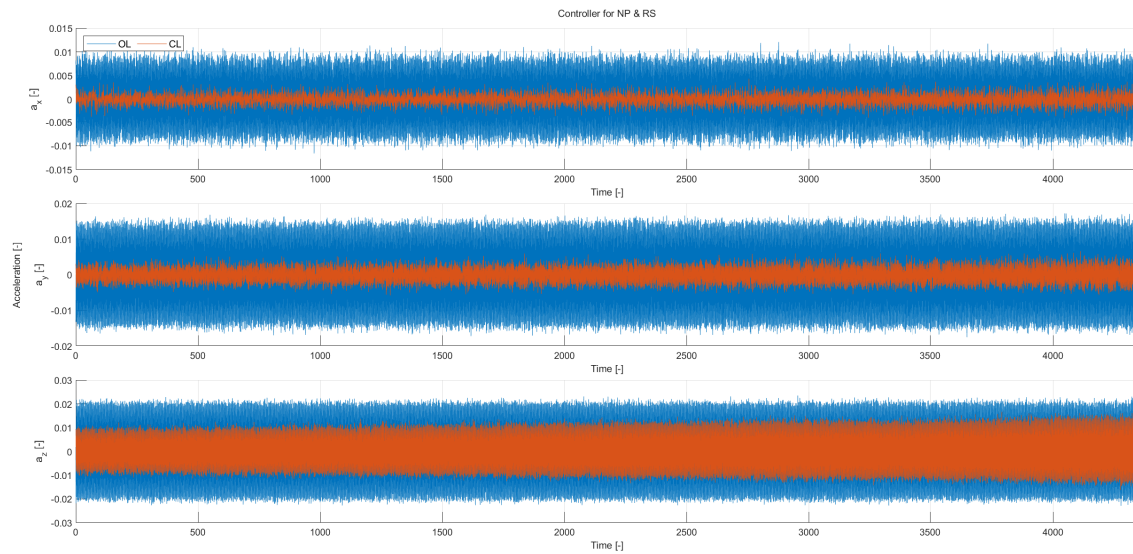


Figure 9.44: Result of test of controller MIMO LTI for NP and RS on the breadboard, disturbance frequency equal to 0.35

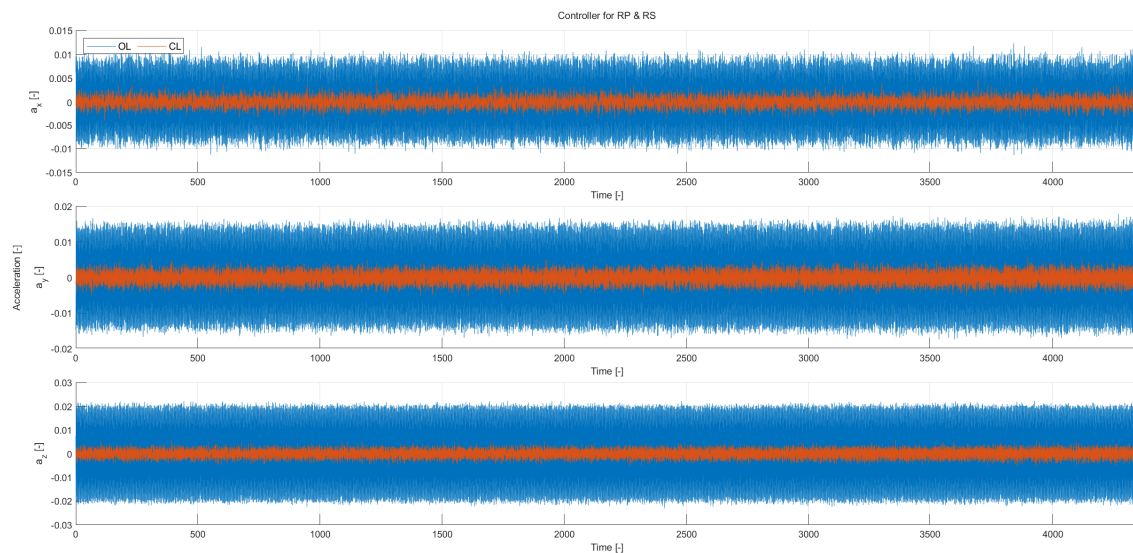


Figure 9.45: Result of test of controller MIMO LTI for RP and RS on the breadboard, disturbance frequency equal to 0.35

9.2.3 SISO LPV control

The MATLAB function `systune` has been used to design a LPV SISO controller. A notch with frequency defined as uncertain parameter in the set $0.27 - 0.31$ was used to design the LPV controller. Because of time constraints, only this frequency range has been considered. Indeed, a single call of the function `systune` can require one or two hours to run, several calls of this function in an iterative process require several hours. The requirements for the controller are reported in Table 9.7. Table 9.8 reports the obtained performance metrics for the controller, n_x is the order of the controller while n_w is the scheduling dimension, i.e. number of repetitions of the linear varying parameter p in the uncertainty $\Delta(p)$ block. Figure 9.46 and Figure 9.47 show the Bode magnitude plot of the sensitivity S and control sensitivity function KS .

Table 9.7: SISO LPV controller design performance weights parameters

Freq. [-]	Goal	W_S base level [dB]	W_S inverse notch depth [dB]	W_S damping factor [-]	W_{KS} DC gain [dB]	W_{KS} high frequency gain [dB]	W_{KS} rolloff frequency [-]
[0.27– 0.31]	NP & RS	−6	17	0.005	−25.62	20	0.64

Table 9.8: SISO LPV controller design performance metrics

Frequency	Goal	Structured	
[0.27 0.31]	NP & RS	n_x	6
		n_w	6
		γ	0.9787
		RS	1.38 Δ

Table 9.9 reports the results of the test in simulation and on the breadboard in terms of open and closed loop acceleration RMS value and reduction ratio. Figure 9.48, Figure 9.49 show the result of the test in simulation of the new controller, in the case the frequency is varied linearly and step-wise, better results are obtained in the latter case. Figure 9.50 shows the result of the test of the con-

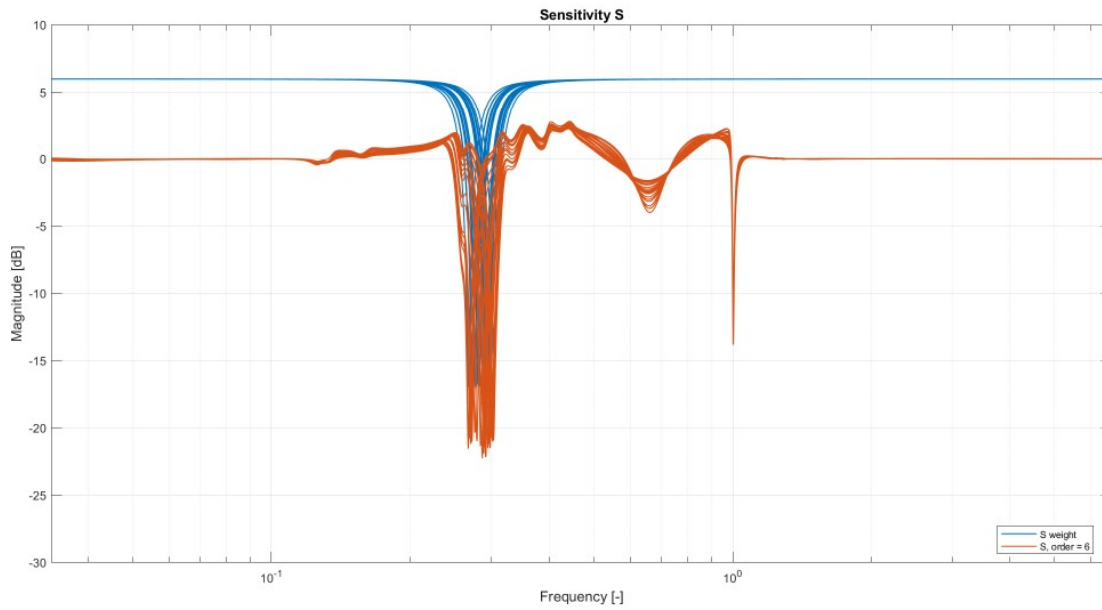


Figure 9.46: Bode magnitude plot of the sensitivity function for the SISO LPV controller

troller on the breadboard. The controller is able to reduce vibration of a factor 4 (12dB).

Table 9.9: Results of simulation and test on breadboard of the SISO LPV controller

Freq.	Goal	Performance metric	Test in simulation		Test on the breadboard
			Varying	Frozen	
[0.27–0.31]	NP & RS	OL RMS [-]	0.0227	0.0227	0.0138
		CL RMS [-]	0.0149	0.0027	0.0036
		ρ [-]	34.32	88.20	73.9
		CL/OL RMS [dB]	-3.65	-18.56	-11.69

9.2. Controllers synthesis and testing

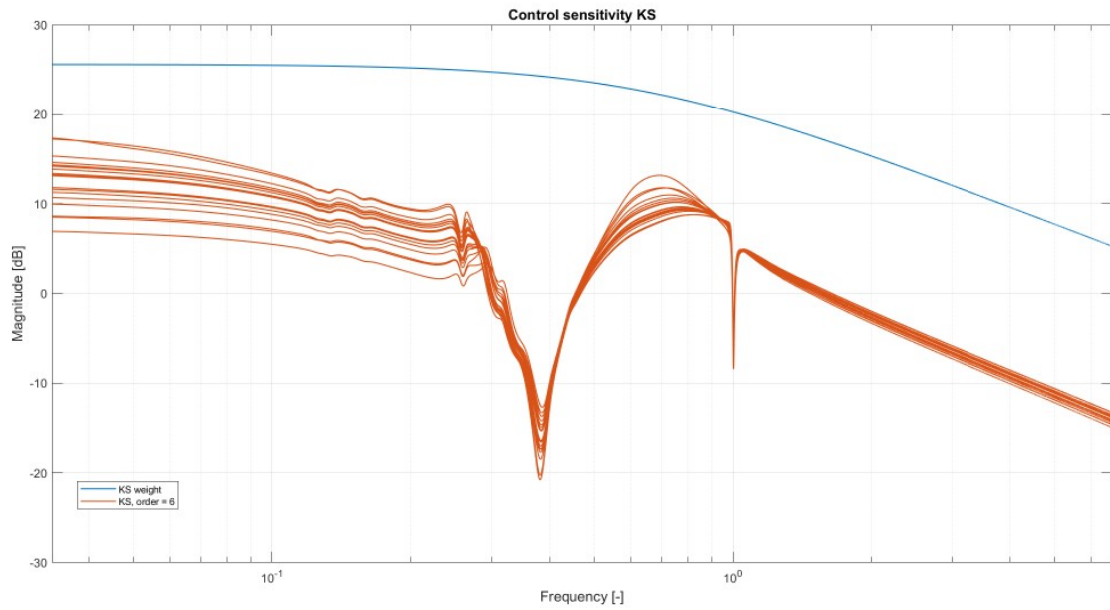


Figure 9.47: Bode magnitude plot of the control sensitivity function for the SISO LPV controller

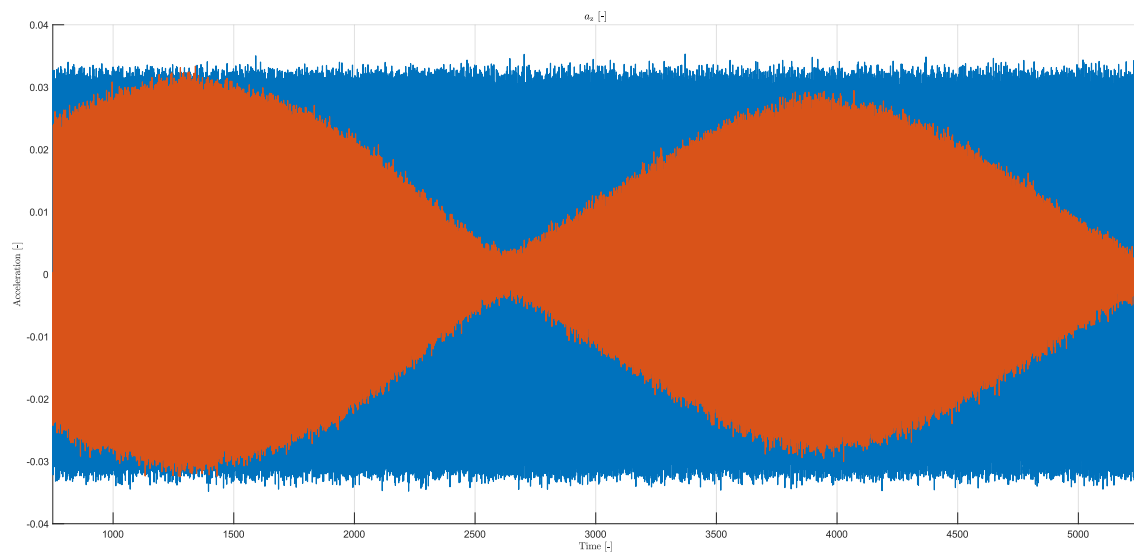


Figure 9.48: Result of test of controller SISO LPV controller in simulation, disturbance frequency varying linearly between 0.27 and 0.31

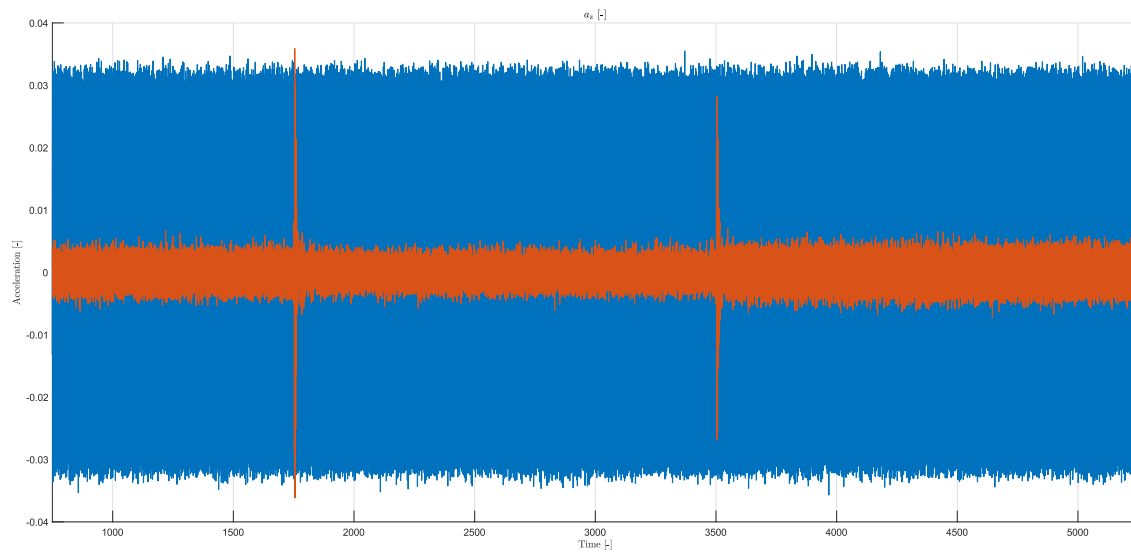


Figure 9.49: Result of test of controller SISO LPV controller in simulation, disturbance frequency varying in steps between 0.27 and 0.31

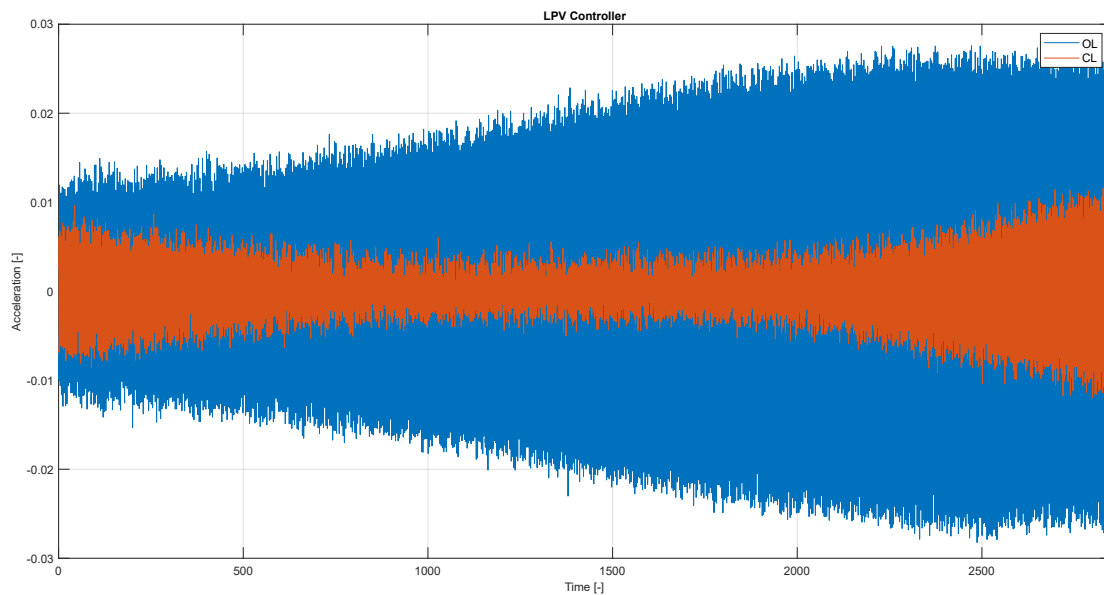


Figure 9.50: Result of test of controller SISO LPV controller on the breadboard, disturbance frequency varying linearly between 0.27 and 0.31

9.2.4 MIMO LPV control

The MATLAB function `systune` has been used to design LPV MIMO controllers. A notch with frequency defined as uncertain parameter in the set $0.27 - 0.31$ was used to design the LPV controller. Because of time constraints, only this frequency range has been considered. Indeed, a single call of the function `systune` for the MIMO system requires up to 8 hours to run, several calls of this function in an iterative process require several hours or days. The requirements for the controller are reported in Table 9.10. Figure 9.51 and Figure 9.52 show the Bode magnitude plot of the sensitivity S and control sensitivity function KS . Table 9.11 reports the performance metrics obtained.

Table 9.10: MIMO LPV controller design performance weights parameters

Freq. [-]	Goal	W_S base level [dB]	W_S inverse notch depth [dB]	W_S damping factor [-]	W_{KS} DC gain [dB]	W_{KS} high frequency gain [dB]	W_{KS} rolloff frequency [-]
[0.27– 0.31]	NP & RS	−6	10	0.005	−25.6	20	0.64

Table 9.11: MIMO LPV controller design performance metrics

Frequency	Goal	Structured	
[0.27 0.31]	NP & RS	n_x	9
		n_w	9
		γ	0.9826
		RS	2.04Δ

Table 9.12 reports the result of the simulation in terms of open loop, closed loop signal RMS and reduction ratio. Figure 9.53 and Figure 9.54 show the result of the test in simulation of the MIMO LPV controller, in the case the frequency is varied linearly and step-wise, better results are obtained in the latter case. The controller has been deployed on the breadboard but the microcontroller encountered an overrun, probably caused by the order of the controller and the dimension of the scheduling variable being too large. It was not possible to syn-

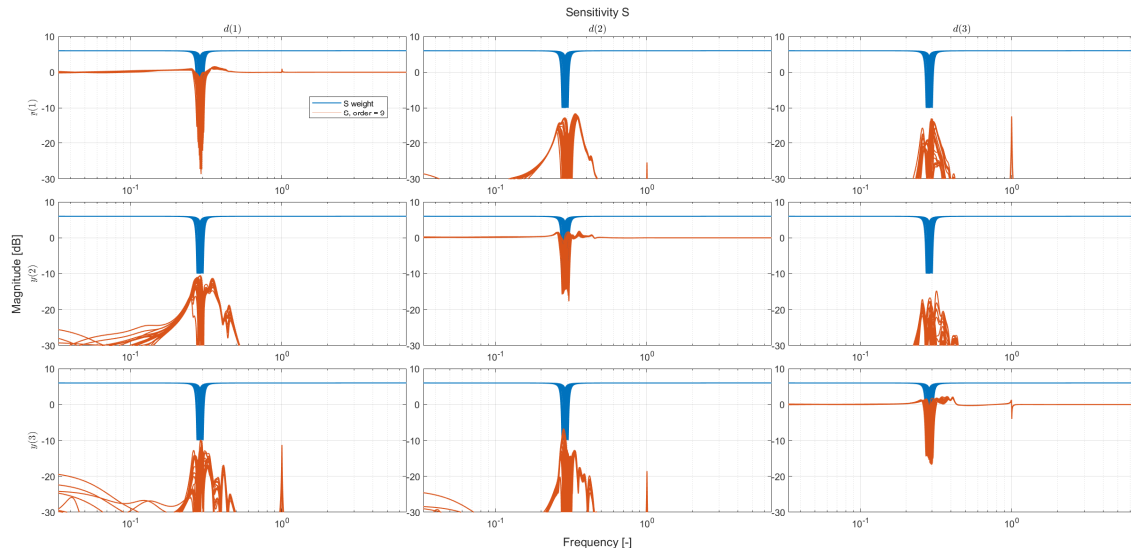


Figure 9.51: Bode magnitude plot of the sensitivity function for the MIMO LPV controller

thesise a lower order controller which achieved the specified performance and which could run on breadboard within the time available.

Table 9.12: Results of simulation for the MIMO LPV controller

Freq.	Goal	Performance metric	Test in simulation					
			Varying			Frozen		
[0.27–0.31]	NP & RS	OL RMS [-]	0.0227	0.0227	0.0227	0.0227	0.0227	0.0227
		CL RMS [-]	0.0189	0.0193	0.0165	0.0043	0.0052	0.0049
		ρ [-]	16.65	14.79	27.13	80.81	76.96	78.26
		CL/OL RMS [dB]	-1.58	-1.39	-2.75	-14.34	-12.75	-13.26

9.2. Controllers synthesis and testing

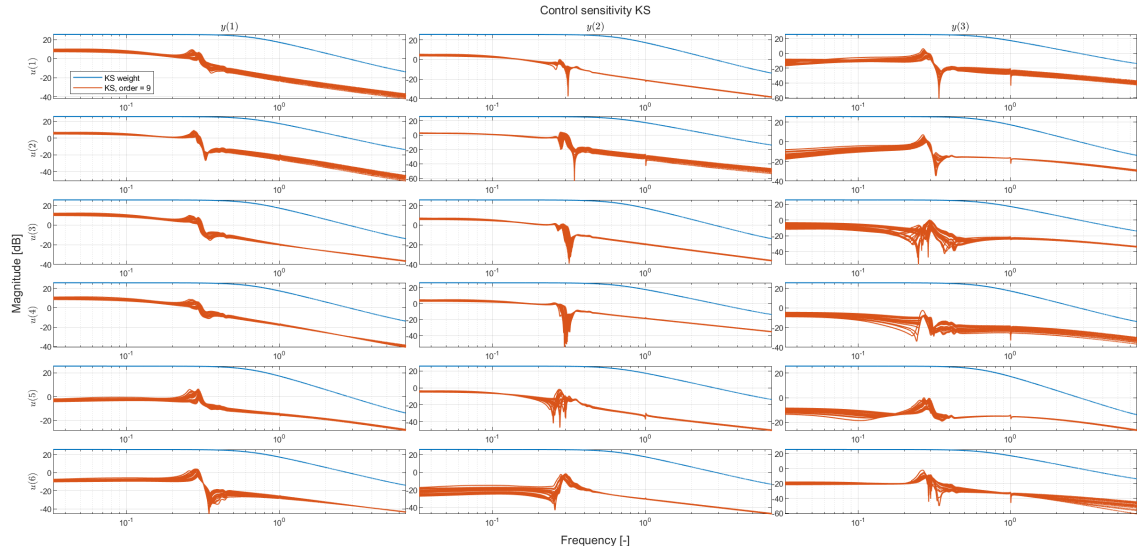


Figure 9.52: Bode magnitude plot of the control sensitivity function for the MIMO LPV controller

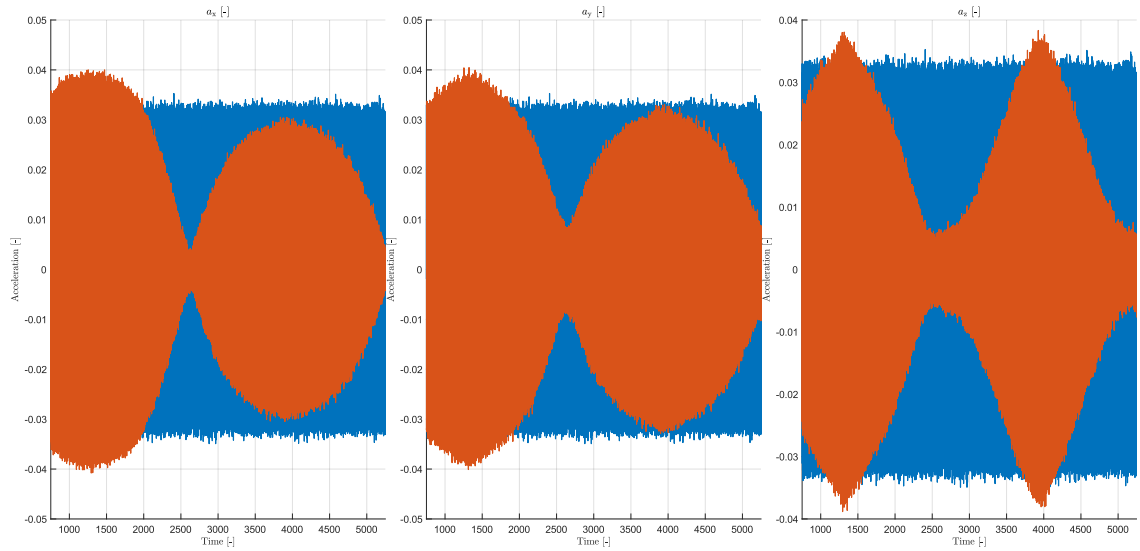


Figure 9.53: Result of test of controller MIMO LPV controller in simulation, disturbance frequency varying linearly between 0.27 and 0.31

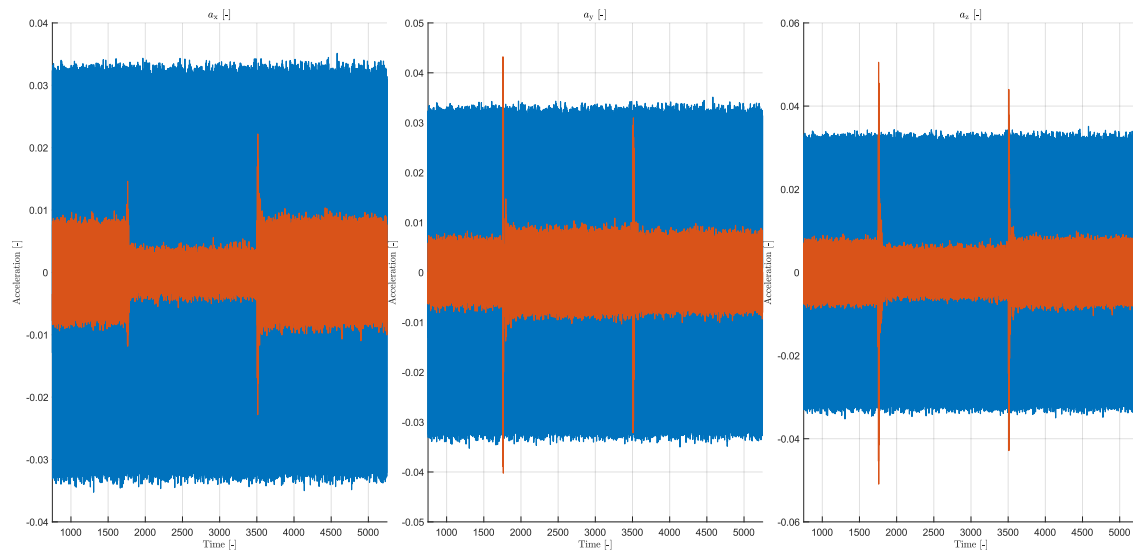


Figure 9.54: Result of test of controller MIMO LPV controller in simulation, disturbance frequency varying in steps between 0.27 and 0.31

9.2.5 Summary of controller synthesis and testing results

Four types of controllers were designed: SISO LTI, MIMO LTI, SISO LPV and MIMO LPV.

The LTI controllers have been also classified according to the performance objective, being it NP (H_∞ -synthesis) and RS or RP (μ -synthesis). Controller order reduction or structured controller synthesis have been used to limit the order of the controller. In the synthesis process, the performance objective and order of the controller were automatically adjusted until the performance criteria were met. For the LPV controllers, the frequency range of disturbance rejection was limited because of the synthesis high computational demand. The obtained closed loop shape was compared to the desired one showing that the performance objectives were met.

The controllers have been tested both in simulation and on the high-stabilisation breadboard. For the LTI control case, there is overlap between the simulation and experimental results. For the SISO LPV control instead, the vibration reduction is greater on the breadboard than in simulation, because of the fixed disturbance's amplitude considered in simulation. Finally, despite the controller order reduction and structured controller synthesis, it was not possible to test the MIMO LPV controller on the hardware. The simulation results show that the disturbance amplitude reduction is achieved for a portion of the considered frequency range.

Table 9.13 ultimately summarises the results of the testing of the different controllers on the breadboard and the testing of the MIMO LPV controller in simulation. The SISO LTI controllers achieve a disturbance's amplitude reduction larger than $20dB$. The MIMO LTI achieves a reduction larger than $14dB$, except for the output component whose amplitude is already close to sensor noise level, where the reduction is limited. The LPV SISO controller achieves an average reduction of $11.7dB$. The MIMO LPV controller achieves an average reduction $1.9dB$ when tested in simulation.

Table 9.13: Results of controllers testing on the breadboard (SISO LTI, MIMO LTI and SISO LPV) and in simulation (MIMO LPV)

Frequency [-]	Goal	Test result		
		x	y	z
SISO LTI				
0.30	NP & RS	/	/	-22.24
	RP & RS	/	/	-23.63
0.42	NP & RS	/	/	-31.44
	RP & RS	/	/	-31.65
MIMO LTI				
0.30	NP & RS	-3.93	-12.48	-16.72
	RP & RS	-5.24	-14.32	-18.69
0.35	NP & RS	-14.83	-13.46	-4.98
	RP & RS	-15.17	-14.48	-17.30
SISO LPV				
[0.27 - 0.31]	NP & RS	/	/	-11.69
MIMO LPV				
[0.27 - 0.31]	NP & RS	-1.58	-1.39	-2.75

9.3 Graphical user interface for system identification and controller synthesis

To make the system identification procedure more user-friendly a GUI was realized using MATLAB appdesigner. The GUI allows the user to:

1. Design the multisine excitation signal;
2. Save it to a JSON file to be used on the breadboard;
3. Perform non-parametric system identification;
4. Perform parametric system identification;
5. Perform model order reduction;
6. Perform uncertainty modelling;

For each step it is possible to plot the results and save them programmatically. The different steps are automatically linked together through directory naming, but the user is free to load results from previous steps from any folder. Figure 9.55 and Figure 9.56 show two screenshots of the system identification GUI.

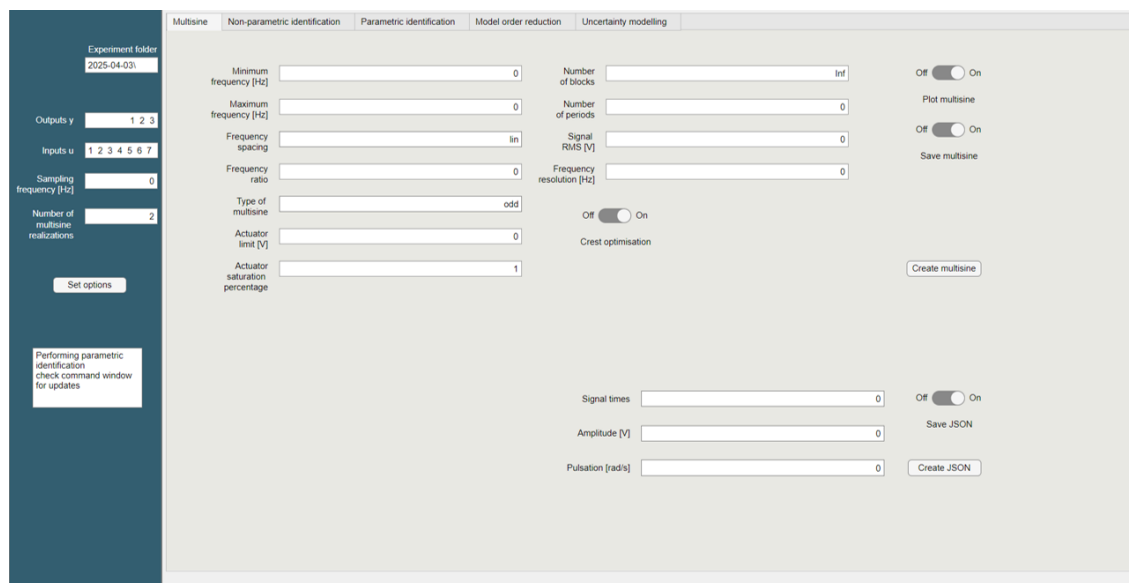


Figure 9.55: Screenshot of the system identification GUI, multisine design initial page

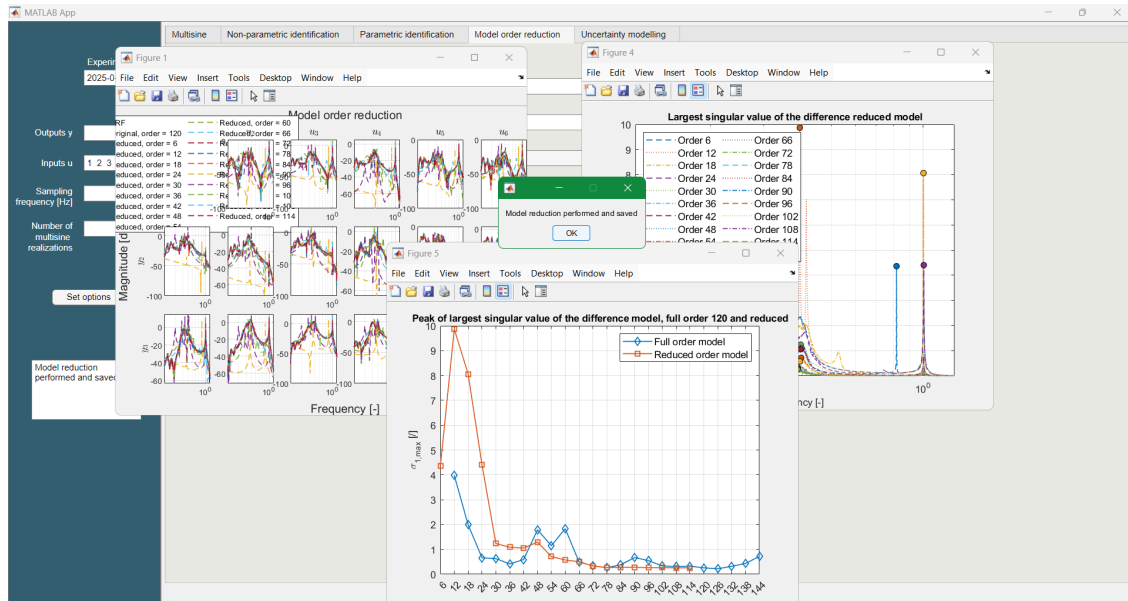


Figure 9.56: Screenshot of the system identification GUI, model order reduction results

A GUI has been realized also for the controller design and test in simulation. The GUI allows the user to:

1. Design a controller using the results of system identification;
2. Design SISO, MIMO LTI controllers of full order, reduced order or structured which achieve NP and RS or RP and RS;
3. Design SISO, MIMO LPV structured controllers which achieve NP and RS or RP and RS;
4. Choose between manual closed loop shaping or semi-automatic closed loop shaping. In the latter case the control sensitivity at low frequency is set based on actuator limit and expected level of disturbance and rolls off at higher frequencies; the notch depth at the expected disturbance frequency is varied until performance objectives are met and down to a level specified by the user; the controller order is varied until performance are met and up to an order specified by the user;
5. Save the controller in a JSON file to be used on the breadboard;
6. Test the controller in simulation

9.3. Graphical user interface for system identification and controller synthesis

For each step it is possible to plot the results and save them programmatically. Figure 9.57 and Figure 9.58 show two screenshots of the control synthesis GUI.

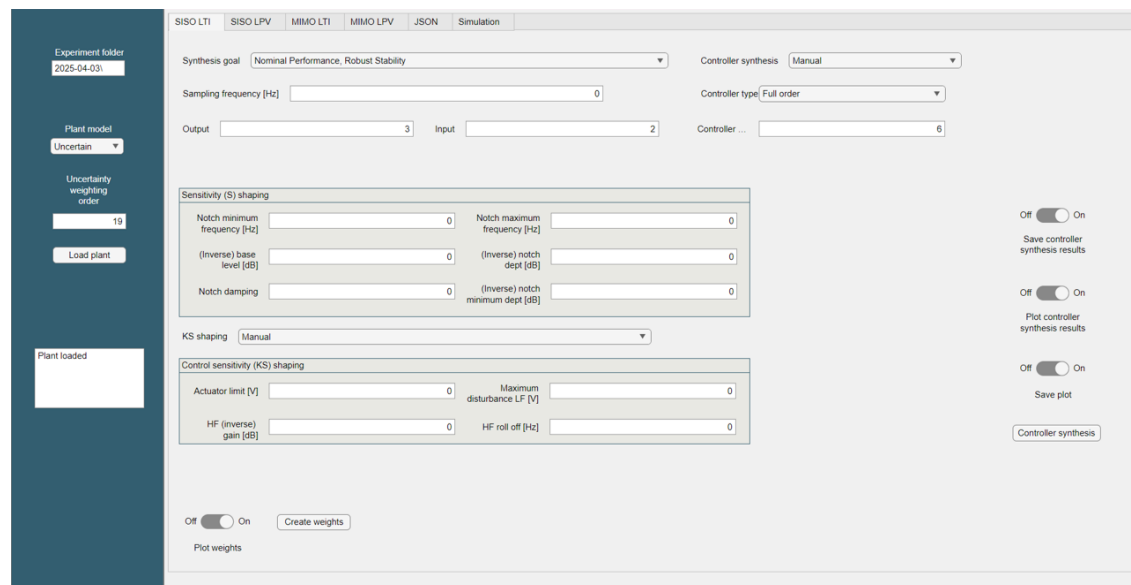


Figure 9.57: Screenshot of the controller synthesis GUI

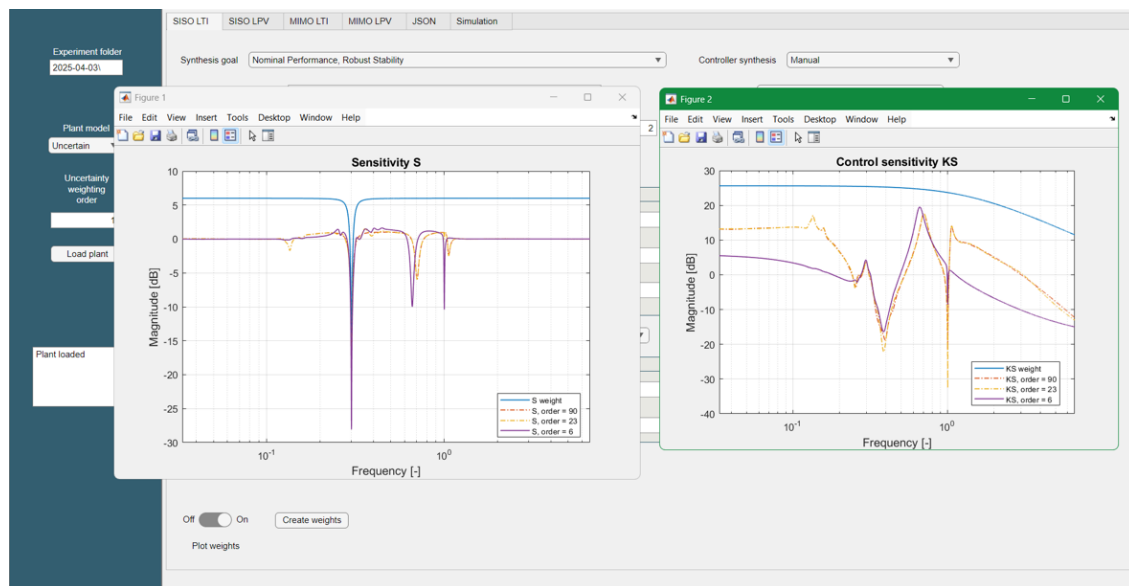


Figure 9.58: Screenshot of the controller synthesis GUI, SISO LTI controller synthesis results

9.3. Graphical user interface for system identification and controller synthesis

10

Part II, Conclusions

Part II of the thesis work regarded the validation of the ESA high-performance stabilisation breadboard. The breadboard is used to test microvibration mitigation techniques to enable fine pointing on small satellites for future space missions.

Modern space missions require increasingly high-precision pointing capabilities, enabled by an ultra-quiet environment. However, subsystems onboard of satellites, such as reaction wheels and cryocoolers, generate microvibrations that can hinder the realisation of such an environment. Microvibration can be even amplified by the light structure of small satellites.

To mitigate this problem, the adoption of a hybrid vibration isolation platform, combining passive and active elements, represents a promising solution. The ESA GNC, AOCS & Pointing Laboratory has developed such a platform, which enables the experimental evaluation of different microvibration compensation strategies. However, each new breadboard configuration required system identification and controller re-tuning, a process that remained largely manual and time-consuming.

To validate the new configuration of the platform and make the process automatic, the system identification and controller synthesis techniques have been tested and integrated into an automatic process. For what concerns system iden-

tification, the controller dynamics results to be characterised by a high number of modes, which confirms the necessity of relying on data-driven methods rather than on first principle modelling. A non-parametric and a linear parametric model of the breadboard have been obtained. To ensure feasible real-time implementation on embedded hardware, model order reduction has also been taken into consideration.

Controllers of different types (LTI, LPV, SISO, MIMO) were designed and experimentally validated. All controllers have been designed using advanced robust control techniques (H-infinity, μ -synthesis), accounting for model uncertainties. Experimental results demonstrated vibration attenuation ranging from a factor of four to a factor of ten or higher, depending on the specific controller and disturbance level. Moreover, the high correlation between experimental data and simulation data demonstrates the effectiveness of the system identification stage.

To enhance usability and repeatability, the entire design and testing workflow has been made automated and user-friendly with a dedicated GUI.

Overall, this work validates the performance of the newly configured hybrid vibration isolation platform and demonstrates the effectiveness of data-driven robust control strategies for microvibration mitigation. The achieved results contribute to advancing active vibration control technologies for next-generation small satellite missions.

11

Conclusions

This thesis work brought contributions to two projects which aim to increase the reliability and performance of small satellites pointing systems:

- the development of a novel testbed architecture for CubeSat ADCS testing installed at ESA/ESTEC, which constitutes Part I of the thesis;
- the validation of a new configuration of the ESA high-performance stabilisation breadboard for fine pointing, which constitutes Part II of the thesis.

The research objectives have been summarised by two research questions, for which an answer is here provided.

11.1 Part I

The development of a new CubeSat attitude testing facility bridged a gap in existing facilities in terms of hosting capability, environment simulation capability, and achievable disturbance torque. This development directly addresses the first research question:

Research Question 1. *“Is it possible to design and realise a CubeSat ADCS test facility capable of hosting CubeSats of different sizes and enabling HIL testing under realistic environmental stimuli?”*

An answer to the Research Question 1 can now be provided:

Proposed Answer. *“A facility for testing the CubeSat ADCS HIL under realistic environmental stimuli can be realised. Different aspects of the space environment can be simulated: the Earth’s magnetic field, the Sun radiation, the low torque environment. CubeSats of sizes from 1U to 12U can be hosted in different configurations. The integration and synchronisation of different sensor stimuli and the mass balancing system allow testing CubeSat ADCS HIL.”*

The mass balancing system is at the core of the answer to the second research question, here recalled:

Research Question 2. *“What level of disturbance torque can be obtained by a facility designed to test the ADCS of CubeSat of different sizes?”*

The following answer is provided to Research Question 2:

Proposed Answer. *“The level of disturbance torque which can be obtained by a facility designed to test the ADCS of CubeSat depends on the size of the device under test. For 1U CubeSats, it can be as high as 3×10^{-5} N m while for CubeSats up to 12U can be as high as 2×10^{-4} N m. The difference lies mainly in the different platform components.”*

11.2 Part II

Part II of the thesis contributed to the validation of the new configuration of the ESA high-performance stabilisation breadboard and the automatization of the system identification and controller synthesis process. This directly addresses

the third research question, here recalled:

Research Question 1. *“What vibration-reduction performance can be achieved through an automated control-tuning process on a hybrid passive-active pointing-stability testbed with an unknown mechanical configuration?”*

An answer can be provided to the Research Question 3:

Proposed Answer. *“The level of vibration-reduction performance that can be achieved on a hybrid passive-active pointing-stability testbed with an unknown mechanical configuration through an automated control-tuning process depends on the controller used and on the type of disturbance. For LTI controllers, which counteract disturbances at a specific frequency, vibrations are reduced, on average, by a factor of 13. For LPV controllers, which counteract disturbance in a frequency range, vibrations are reduced, on average, by a factor of 4. In the latter case, only the performance of the SISO LPV controller could be evaluated on the breadboard, while the LPV MIMO controller was only tested in simulation.”*

11.3 Ideas for future development

The CubeDynA facility is going through an update to test CubeSats of size up to 16U, further enhancing its capabilities. The facility has proven to be a valid tool for the testing of CubeSat ADCS HIL, but improvements in different directions can be undertaken. In particular, the performance of the balancing system could be further improved by:

- employing more accurate sensors to be used during the mass balancing process;
- possibly integrate the attitude estimate from the ground-truth in the balancing process, enhancing its accuracy by employing a higher number of cameras;

- employing linear actuators with micro-stepping to further improve the mass balancing resolution;
- explore new system identification techniques for inertia and offset estimation;
- realise a digital-twin of the facility to speed up the balancing process;

Clearly, each of the proposed solutions' impact shall be evaluated against time, cost and facility size constraints.

For what concerns the high-performance stabilisation breadboard, the following areas of improvement are identified:

- performing the system identification in a narrower frequency range to obtain a lower order model and possibly an implementable LPV MIMO controller;
- further automatize steps of the system identification and controller synthesis process;
- include also the noise covariance estimation of the FRF in the uncertainty modelling;

Also here, a trade-off between implementation complexity and achieved results shall be conducted.

Overall, both systems constitute solid tools to enhance future small satellite missions by improving their reliability and performance.

Appendices



Coarse mass balancing calculations

Case 1

- 1U CubeSat with 2 kg mass
- CubeSat in vertical orientation
- CubeSat CM position at +2 cm from its GC
- GSE of 2 kg mass with CM position at +2 cm from the CR
- Mass balancing mass mounted on the pins at 2 cm distance from the intersection between pin and platform

In this case 1.595 kg of counterweights are needed, which can be obtained with 8 disks of 0.2 kg each. The different components contribution to the mass balancing is shown in Figure A.1.

The total resulting mass is of 10.904 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
1U CubeSat	2000	76.75	DUT base	180100
GSE 1	2000	20.00	Platform plane	46600
GSE 2	0	0.00	Platform plane	0
1U Platform	3865	-22.06	Platform plane	-72517
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	1595	-20	Platform bottom	-106372
Counterweights/8	199			
Total mass	10904		SUM	0

Figure A.1: Coarse mass balancing result, case 1

Case 2

- 1U CubeSat with 1 kg mass
- CubeSat in vertical orientation
- CubeSat CM position at -2 cm from its GC
- No GSE
- Spacers 1, 2 and 3 are used to shift up the CM position
- Mass balancing mass mounted on the pins at 1.6 cm distance from the intersection between pin and platform

In this case 0.792 kg of counterweights are needed, which can be obtained with 8 disks of 0.1 kg each. The different components contribution to the mass balancing is shown in Figure A.2.

The total resulting mass is of 7.101 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 1U	1000	36.75	DUT base	71250
GSE 1	0	0.00	Platform plane	0
GSE 2	0	0.00	Platform plane	0
1U Platform	3865	-22.06	Platform plane	9421
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	792	-16	Platform bottom	-32860
Counterweights/8	99			
Total mass	7101		SUM	0

Figure A.2: Coarse mass balancing result, case 2

Case 3

- 1U CubeSat with 2 kg mass
- CubeSat in horizontal orientation
- CubeSat CM position at $+2$ cm from its GC
- GSE of 2 kg mass with CM position at $+2$ cm from the CR

- Mass balancing mass mounted on the pins at 1.8 cm distance from the intersection between pin and platform

In this case 1.590 kg of counterweights are needed, which can be obtained with 8 disks of 0.2 kg each. The different components contribution to the mass balancing is shown in Figure A.3.

The total resulting mass is of 10.899 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 1U	2000	70.00	DUT base	176600
GSE 1	2000	20.00	Platform plane	46600
GSE 2	0	0.00	Platform plane	0
1U Platform	3865	-22.06	Platform plane	-72517
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	1590	-18	Platform bottom	-102872
Counterweights/8	199			
Total mass	10899		SUM	0

Figure A.3: Coarse mass balancing result, case 3

Case 4

- 1U CubeSat with 1 kg mass
- CubeSat in horizontal orientation
- CubeSat CM position at -2 cm from its GC
- No GSE
- Spacers 1, 2 and 3 are used to shift up the CM position
- Mass balancing mass mounted on the pins at 1.3 cm distance from the intersection between pin and platform

In this case 0.808 kg of counterweights are needed, which can be obtained with 8 disks of 0.1 kg each. The different components contribution to the mass balancing is shown in Figure A.4.

The total resulting mass is of 7.117 kg.

Case 5

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 1U	1000	30.00	DUT base	69500
GSE 1	0	0.00	Platform plane	0
GSE 2	0	0.00	Platform plane	0
1U Platform	3865	-22.06	Platform plane	9421
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	808	-13	Platform bottom	-31110
Counterweights/8	101			
Total mass	7117		SUM	0

Figure A.4: Coarse mass balancing result, case 4

- SpaceInventor with 2 kg mass
- SpaceInventor CM position at +5.19 cm position from the platform upper surface
- GSE of 2 kg mass with CM position at +2 cm from the CR
- Mass balancing mass mounted on the pins at 3.5 cm distance from the intersection between pin and platform

In this case 0.449 kg of counterweights are needed, which can be obtained with 8 disks of 0.05 kg each. The different components contribution to the mass balancing is shown in Figure A.5.

The total resulting mass is of 9.758 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
SpaceInventor	2000	51.90	DUT base	110406
GSE 1	2000	20.00	Platform plane	46600
GSE 2	0	0.00	Platform plane	0
1U Platform	3865	-22.06	Platform plane	-72517
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	449	-35	Platform bottom	-36678
Counterweights/8	56			
Total mass	9758		SUM	0

Figure A.5: Coarse mass balancing result, case 5

Case 6

- 3U CubeSat with 6 kg mass
- CubeSat in vertical orientation
- CubeSat CM position at +7 cm from its GC
- GSE of 2 kg mass with CM position at +2 cm from the CR
- Mass balancing mass mounted on the pins at 8.5 cm distance from the intersection between pin and platform

In this case 10.510 kg of counterweights are needed, which can be obtained with 8 disks of 1 kg, 8 disks of 0.2 kg and 8 disks of 0.1 kg each. The different components contribution to the mass balancing is shown in Figure A.6.

The total resulting mass is of 31.307 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
Cubesat 3U	6000	240.25	DUT base	1531500
GSE 1	2000	20.00	Platform plane	50000
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	10510	-85	Platform bottom*	-1418797
Counterweights/8	1314			
Total mass	31307			

Figure A.6: Coarse mass balancing result, case 6

Case 7

- 3U CubeSat with 3 kg mass
- CubeSat in vertical orientation
- CubeSat CM position at -7 cm from its GC
- No GSE
- Mass balancing mass mounted on the pins at 6.1 cm distance from the intersection between pin and platform

In this case 1.608 kg of counterweights are needed, which can be obtained with 8 disks of 0.2 kg each. The different components contribution to the mass balancing is shown in Figure A.7.

The total resulting mass is of 17.405 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
Cubesat 3U	3000	100.25	DUT base	345750
GSE 1	0	0.00	Platform plane	0
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	1608	-61	Platform bottom*	-183047
Counterweights/8	201			
Total mass	17405			

Figure A.7: Coarse mass balancing result, case 7

Case 8

- 3U CubeSat with 6 kg mass
- CubeSat in horizontal orientation
- CubeSat CM position at +2 cm from its GC
- GSE of 2 kg mass with CM position at +2 cm from the CR
- Mass balancing mass mounted on the pins at 5.3 cm distance from the intersection between pin and platform

In this case 4.002 kg of counterweights are needed, which can be obtained with 8 disks of 0.5 kg each. The different components contribution to the mass balancing is shown in Figure A.8.

The total resulting mass is of 24.799 kg.

Case 9

- 3U CubeSat with 3 kg mass
- CubeSat in horizontal orientation
- CubeSat CM position at -2 cm from its GC

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 3U	6000	70.00	DUT base	540000
GSE 1	2000	20.00	Platform plane	50000
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	4002	-53	Platform bottom*	-427297
Counterweights/8	500			
Total mass	24799			

Figure A.8: Coarse mass balancing result, case 8

- No GSE
- Spacer 1 used to shift up CM position
- Mass balancing mass mounted on the pins at 2.8 cm distance from the intersection between pin and platform

In this case 0.798 kg of counterweights are needed, which can be obtained with 8 disks of 0.1 kg each. The different components contribution to the mass balancing is shown in Figure A.9.

The total resulting mass is of 16.595 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 3U	3000	30.00	DUT base	165900
GSE 1	0	0.00	Platform plane	0
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-54721
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	798	-28	Platform bottom*	-63368
Counterweights/8	100			
Total mass	16595			

Figure A.9: Coarse mass balancing result, case 9

Case 10

- 6U CubeSat with 12 kg mass

- CubeSat in vertical orientation
- CubeSat CM position at +7 cm from its GC
- GSE of 2 kg mass with CM position at +2 cm from the CR
- Mass balancing mass mounted on the pins at 8.1 cm distance from the intersection between pin and platform

In this case 24.062 kg of counterweights are needed, which can be obtained with 8 disks of 2 kg and 8 disks of 1 kg each. The different components contribution to the mass balancing is shown in Figure A.10.

The total resulting mass is of 50.859 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
Cubesat 6U	12000	253.00	DUT base	3276000
GSE 1	2000	20.00	Platform plane	50000
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	24062	-81	Platform bottom*	-3163297
Counterweights/8	3008			
Total mass	50859			

Figure A.10: Coarse mass balancing result, case 10

Case 11

- 6U CubeSat with 6 kg mass
- CubeSat in vertical orientation
- CubeSat CM position at –2 cm from its GC
- No GSE
- Mass balancing mass mounted on the pins at 8.2 cm distance from the intersection between pin and platform

In this case 4.800 kg of counterweights are needed, which can be obtained with 8 disks of 0.5 kg and 8 disks of 0.1 kg each. The different components contribution to the mass balancing is shown in Figure A.11.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
Cubesat 6U	6000	113.00	DUT base	798000
GSE 1	0	0.00	Platform plane	0
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	4800	-82	Platform bottom*	-635297
Counterweights/8	600			
Total mass	23597			

Figure A.11: Coarse mass balancing result, case 11

The total resulting mass is of 23.597 kg.

Case 12

- 6U CubeSat with 12 kg mass
- CubeSat in horizontal orientation with its x axis aligned with platform z axis
- CubeSat CM position at +4.5 cm from its GC
- GSE of 2 kg mass with CM position at +2 cm from the CR
- Mass balancing mass mounted on the pins at 7.5 cm distance from the intersection between pin and platform

In this case 16.055 kg of counterweights are needed, which can be obtained with 8 disks of 2 kg each. The different components contribution to the mass balancing is shown in Figure A.12.

The total resulting mass is of 42.852 kg.

Case 13

- 6U CubeSat with 6 kg mass
- CubeSat in horizontal orientation with its y axis aligned with platform z axis
- CubeSat CM position at -2 cm from its GC
- No GSE

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 6U	12000	158.15	DUT base	2138400
GSE 1	2000	20.00	Platform plane	50000
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	16055	-75	Platform bottom*	-2025697
Counterweights/8	2007			
Total mass	42852			

Figure A.12: Coarse mass balancing result, case 12

- Mass balancing mass mounted on the pins at 3 cm distance from the intersection between pin and platform

In this case 1.591 kg of counterweights are needed, which can be obtained with 8 disks of 0.2 kg each. The different components contribution to the mass balancing is shown in Figure A.13.

The total resulting mass is of 20.388 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 6U	6000	30.00	DUT base	300300
GSE 1	0	0.00	Platform plane	0
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	1591	-30	Platform bottom*	-137597
Counterweights/8	199			
Total mass	20388			

Figure A.13: Coarse mass balancing result, case 13

Case 14

- 12U CubeSat with 24 kg mass
- CubeSat in vertical orientation
- CubeSat CM position at +7 cm from its GC

- GSE of 2 kg mass with CM position at +2 cm from the CR
- Mass balancing mass mounted on the pins at 16.6 cm distance from the intersection between pin and platform. Extended pin disk used.

In this case 31.189 kg of counterweights are needed, which can be obtained with 8 disks of 2 kg, 8 disks of 1 kg, 8 disks of 0.6 kg (extended pin disk), 8 disks of 0.2 kg and 8 disks of 0.1 kg each. The different components contribution to the mass balancing is shown in Figure A.14.

The total resulting mass is of 69.986 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 12U	24000	253.00	DUT base	6552000
GSE 1	2000	20.00	Platform plane	50000
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	31189	-166	Platform bottom*	-6439297
Counterweights/8	3899			
Total mass	69986			

Figure A.14: Coarse mass balancing result, case 14

Case 15

- 12U CubeSat with 12 kg mass
- CubeSat in vertical orientation
- CubeSat CM position at -7 cm from its GC
- No GSE
- Mass balancing mass mounted on the pins at 8.8 cm distance from the intersection between pin and platform

In this case 10.413 kg of counterweights are needed, which can be obtained with 8 disks of 1 kg, 8 disks of 0.2 kg and 8 disks of 0.1 kg each. The different components contribution to the mass balancing is shown in Figure A.15.

The total resulting mass is of 35.210 kg.

Case 16

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 12U	12000	113.00	DUT base	1596000
GSE 1	0	0.00	Platform plane	0
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	10413	-88	Platform bottom*	-1433297
Counterweights/8	1302			
Total mass	35210			

Figure A.15: Coarse mass balancing result, case 15

- 12U CubeSat with 24 kg mass
- CubeSat in horizontal orientation
- CubeSat CM position at +4.5 cm from its GC
- GSE of 2 kg mass with CM position at +2 cm from the CR
- Mass balancing mass mounted on the pins at 15.0 cm distance from the intersection between pin and platform. Extended pin disk used.

In this case 21.659 kg of counterweights are needed, which can be obtained with 8 disks of 2 kg, 8 disks of 0.6 kg (extended pin disk) and 8 disks of 0.1 kg each. The different components contribution to the mass balancing is shown in Figure A.16.

The total resulting mass is of 60.446 kg.

Case 17

- 12U CubeSat with 12 kg mass
- CubeSat in horizontal orientation
- CubeSat CM position at -4.5 cm from its GC
- No GSE
- Mass balancing mass mounted on the pins at 9.0 cm distance from the intersection between pin and platform

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
CubeSat 12U	24000	158.15	DUT base	4276800
GSE 1	2000	20.00	Platform plane	50000
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	21649	-150	Platform bottom*	-4164097
Counterweights/8	2706			
Total mass	60446			

Figure A.16: Coarse mass balancing result, case 16

In this case 6.425 kg of counterweights are needed, which can be obtained with 8 disks of 0.5 kg, 8 disks of 0.2 kg and 8 disks of 0.1 kg each. The different components contribution to the mass balancing is shown in Figure A.17.

The total resulting mass is of 31.222 kg.

Component	Mass [g]	CM vert [mm]	Relative to	Moment vert [g*mm]
Cubesat 12U	12000	68.15	DUT base	1058400
GSE 1	0	0.00	Platform plane	0
GSE 2	0	0.00	Platform plane	0
3U-12U Platform	11353	-15.12	Platform plane	-114892
Air bearing	1444	-33.11	Air bearing top	-47811
Counterweights total	6425	-90	Platform bottom*	-895697
Counterweights/8	803			
Total mass	31222			

Figure A.17: Coarse mass balancing result, case 17

Bibliography

- G. S. Aglietti, R. S. Langley, E. Rogers, and S. B. Gabriel. Model building and verification for active control of microvibrations with probabilistic assessment of the effects of uncertainties. *Proceedings of the Institution of Mechanical Engineers, Part C: Journal of Mechanical Engineering Science*, 218(4):389–399, Apr. 2004. ISSN 0954-4062, 2041-2983. doi: 10.1177/095440620421800404. URL <https://journals.sagepub.com/doi/10.1177/095440620421800404>.
- B. N. Agrawal and R. E. Rasmussen. Air-bearing-based satellite attitude dynamics simulator for control software research and development. pages 204–214, Orlando, FL, 2001. doi: 10.1117/12.438072. URL <http://proceedings.spiedigitallibrary.org/proceeding.aspx?articleid=912729>.
- M. I. Al-Majed and B. N. Alsuwaidan. A new testing platform for attitude determination and control subsystems: Design and applications. In *2009 IEEE/ASME International Conference on Advanced Intelligent Mechatronics*, pages 1318–1323, Singapore, 2009. IEEE. ISBN 978-1-4244-2852-6. doi: 10.1109/AIM.2009.5229871. URL <http://ieeexplore.ieee.org/document/5229871/>.
- Astrofein. Astro-und Feinwerktechnik Adlershof GmbH NanoSat Test Bed, 2025. URL <https://www.astrofein.com/en/products/acs-test-stand/>.
- A. Bahu. Development of a ground testing facility and attitude control for magnetically actuated nanosatellites. 2021. Publisher: alma.
- A. Bahu and D. Modenini. Automatic mass balancing system for a dynamic CubeSat attitude simulator: development and experimental validation. *CEAS*

- Space Journal*, 12(4):597–611, Dec. 2020. ISSN 1868-2502, 1868-2510. doi: 10.1007/s12567-020-00309-5. URL <https://link.springer.com/10.1007/s12567-020-00309-5>.
- D. Bhatia. Attitude Determination and Control System Design of Sub-Arcsecond Pointing Spacecraft. *Journal of Guidance, Control, and Dynamics*, 44(2):295–314, Feb. 2021. ISSN 1533-3884. doi: 10.2514/1.G005116. URL <https://arc.aiaa.org/doi/10.2514/1.G005116>.
- S. Blair. Telstar at 50. *Engineering & Technology*, 7(7):82–85, Aug. 2012. ISSN 1750-9637, 1750-9645. doi: 10.1049/et.2012.0716. URL <http://digital-library.theiet.org/doi/10.1049/et.2012.0716>.
- T. Bloemers, S. Leemrijse, V. Preda, F. Boquet, T. Oomen, and R. Tóth. Vibration Control Under Frequency-Varying Disturbances With Application to Satellites. *IEEE Transactions on Control Systems Technology*, 32(6):1983–1994, Nov. 2024. ISSN 1063-6536, 1558-0865, 2374-0159. doi: 10.1109/TCST.2024.3384896. URL <https://ieeexplore.ieee.org/document/10507204/>.
- J. Bouwmeester, A. Menicucci, and E. Gill. Improving CubeSat reliability: Subsystem redundancy or improved testing? *Reliability Engineering & System Safety*, 220:108288, Apr. 2022. ISSN 09518320. doi: 10.1016/j.res.2021.108288. URL <https://linkinghub.elsevier.com/retrieve/pii/S0951832021007584>.
- R. Boynton. Using a spherical air bearing to simulate weightlessness. *Weight Engineering*, 56(2):37–47, 1996.
- R. A. Braeunig. Properties of Standard Atmosphere, 2025. URL <http://www.braeunig.us/space/atmos.htm>.
- Broadcom Corporation. BCM2835 ARM Peripherals. Technical report, Raspberry Pi Foundation, Feb. 2012. URL <https://www.raspberrypi.org/app/uploads/2012/02/BCM2835-ARM-Peripherals.pdf>.
- A. J. Bronowicki. Vibration Isolator for Large Space Telescopes. *Journal of Spacecraft and Rockets*, 43(1):45–53, Jan. 2006. ISSN 0022-4650, 1533-6794. doi: 10.2514/1.12036. URL <https://arc.aiaa.org/doi/10.2514/1.12036>.

- BryceTech. Start-Up Space 2023: Update on investment in commercial space ventures. Technical report, 2023. URL https://brycetech.com/reports/report-documents/Bryce_Start_Up_Space_2023.pdf.
- BST. Berlin Space Technologies ADCS simulator, 2024. URL <https://www.berlin-space-tech.com/adcs/>.
- <. Burkhard. Platforms Presentation. Presentation / NASA Technical Report 20210021952, NASA Technical Reports Server (NTRS), United States, 2021. URL https://ntrs.nasa.gov/api/citations/20210021952/downloads/Platforms%20Presentation_Burkhard_FNL.pdf.
- K. Cahoy, P. Grenfell, A. Crews, M. Long, P. Serra, A. Nguyen, R. Fitzgerald, C. Haughwout, R. Diez, A. Aguilar, J. Conklin, C. Payne, J. Kusters, C. Sackier, M. LaRocca, and L. Yenchesky. The CubeSat Laser Infrared Crosslink Mission (CLICK). In N. Karafolas, Z. Sodnik, and B. Cugny, editors, *International Conference on Space Optics — ICSO 2018*, page 33, Chania, Greece, July 2019. SPIE. ISBN 978-1-5106-3077-2 978-1-5106-3078-9. doi: 10.1117/12.2535953. URL <https://www.spiedigitallibrary.org/conference-proceedings-of-spie/11180/2535953/The-CubeSat-Laser-Infrared-Crosslink-Mission-CLICK/10.1117/12.2535953.full>.
- I. Cantiello, A. Hyslop, R. Blommesteijn, R. Walker, C. Pirat, and T. Szewczyk. ESA Lessons Learned & Guidelines on Nano - SmallSat Functional Verification. 2022. doi: 10.13140/RG.2.2.17164.49284. URL <https://rgdoi.net/10.13140/RG.2.2.17164.49284>. Publisher: Unpublished.
- CASGC, C. on Achieving Science Goals with CubeSats, Space Studies Board, Division on Engineering and Physical Sciences, and National Academies of Sciences, Engineering, and Medicine. *Achieving Science with CubeSats: Thinking Inside the Box*. National Academies Press, Washington, D.C., 2016. ISBN 978-0-309-44263-3. doi: 10.17226/23503. URL <https://www.nap.edu/catalog/23503>. Pages: 23503.
- CCSDS. Consultative Committee for Space Data Systems Space Packet Protocol. Recommended Standard CCSDS 133.0-B-2, Consultative Committee for Space

- Data Systems, Washington, DC, USA, June 2020. URL <https://ccsds.org/Pubs/133x0b2e2.pdf>.
- CCSDS. Consultative Committee for Space Data Systems Overview of Space Communications Protocols. Informational Report CCSDS 130.0-G-4, Consultative Committee for Space Data Systems, Washington, DC, USA, Apr. 2023. URL <https://ccsds.org/Pubs/130x0g4e1.pdf>.
- P. Chayer, S. Holfeltz, E. Nelan, J. Hutchings, R. Doyon, and N. Rowlands. JWST Fine Guidance Sensor Calibration. *Hubble after SM4. Preparing JWST*, page 59, 2010.
- S. Chesi, Q. Gong, V. Pellegrini, R. Cristi, and M. Romano. Automatic Mass Balancing of a Spacecraft Three-Axis Simulator: Analysis and Experimentation. *Journal of Guidance, Control, and Dynamics*, 37(1):197–206, Jan. 2014. ISSN 0731-5090, 1533-3884. doi: 10.2514/1.60380. URL <https://arc.aiaa.org/doi/10.2514/1.60380>.
- S. Chesi, O. Perez, and M. Romano. A Dynamic Hardware-in-the-loop Three-Axis Simulator of Nanosatellite Dimensions. *Journal of Small Spacecraft*, 4: 315–328, 2015.
- J. P. Contreras, N. A. Arestegui, E. H. Cayo, and E. M. Pumacota. Low Weight and Inertia Self-Balancing Testbed for a 3U CubeSat Attitude Control System. *IEEE Access*, 13:140392–140411, 2025. ISSN 2169-3536. doi: 10.1109/ACCESS.2025.3597232. URL <https://ieeexplore.ieee.org/document/11121164/>.
- A. Curatolo, A. Bahu, and D. Modenini. Automatic Balancing for Satellite Simulators with Mixed Mechanical and Magnetic Actuation. *Aerospace*, 9(4): 223, Apr. 2022. ISSN 2226-4310. doi: 10.3390/aerospace9040223. URL <https://www.mdpi.com/2226-4310/9/4/223>.
- R. F. Da Costa, O. Saotome, and E. Rafikova. Efficacy Comparison of Satellite Attitude Control Systems Using a Spherical Air Bearing. In Y. Iano, R. Arthur, O. Saotome, V. Vieira Estrela, and H. J. Loschi, editors, *Proceedings of the 4th Brazilian Technology Symposium (BTSym'18)*, volume 140, pages 55–64. Springer International Publishing, Cham, 2019. ISBN 978-3-030-16052-4 978-3-030-16053-1. doi: 10.1007/978-3-030-16053-1_6. URL

- http://link.springer.com/10.1007/978-3-030-16053-1_6. Series Title: Smart Innovation, Systems and Technologies.
- A. Das, J. Berg, G. Norris, D. Cossey, T. Strange, and W. Schlaegel. ASTREX-a unique test bed for CSI research. In *29th IEEE Conference on Decision and Control*, pages 2018–2023 vol.4, Honolulu, HI, USA, 1990. IEEE. doi: 10.1109/CDC.1990.203978. URL <http://ieeexplore.ieee.org/document/203978/>.
- R. A. de Callafon, D. de Roover, and P. M. J. Van den Hof. Multivariable least squares frequency domain identification using polynomial matrix fraction descriptions. In *Proceedings of 35th IEEE Conference on Decision and Control*, pages 2030–2035 vol.2, Kobe, Japan, 1996. doi: 10.1109/CDC.1996.572883.
- A. H. J. De Ruiter, C. Damaren, and J. R. Forbes. *Spacecraft dynamics and control: an introduction*. Wiley, Chichester, West Sussex, UK, 2013. ISBN 978-1-118-40332-7 978-1-118-40330-3 978-1-118-40331-0.
- F. L. De Sousa. Are smallsats taking over bigsats for land Earth observation? *Acta Astronautica*, 213:455–463, Dec. 2023. ISSN 00945765. doi: 10.1016/j.actaastro.2023.09.041. URL <https://linkinghub.elsevier.com/retrieve/pii/S0094576523005064>.
- T. Demerville and P. Bastia. Original Elastomeric Dampers for the EXPERT Reentry Vehicle. *ESA ESMATS, Germany*, 2011.
- G. Denis, D. Alary, X. Pasco, N. Pisot, D. Texier, and S. Toulza. From new space to big space: How commercial space dream is becoming a reality. *Acta Astronautica*, 166:431–443, Jan. 2020. ISSN 00945765. doi: 10.1016/j.actaastro.2019.08.031. URL <https://linkinghub.elsevier.com/retrieve/pii/S0094576519313451>.
- DINGS’. DINGS’ USA DLM Series, 2022. URL <https://www.dingsmotion.com/products/products-middle-12DLM.php>.
- E. Dotto, V. Della Corte, M. Amoroso, I. Bertini, J. Brucato, A. Capannolo, B. Cotugno, G. Cremonese, V. Di Tana, I. Gai, S. Ieva, G. Impresario, S. Ivanovski, M. Lavagna, A. Lucchetti, E. Mazzotta Epifani, A. Meneghin, F. Miglioretti, D. Modenini, M. Pajola, P. Palumbo, D. Perna, S. Pirrotta, G. Poggiali, A. Rossi,

- E. Simioni, S. Simonetti, P. Tortora, M. Zannoni, G. Zanotti, A. Zinzi, A. Cheng, A. Rivkin, E. Adams, E. Reynolds, and K. Fretz. LICIACube - The Light Italian Cubesat for Imaging of Asteroids In support of the NASA DART mission towards asteroid (65803) Didymos. *Planetary and Space Science*, 199: 105185, May 2021. ISSN 00320633. doi: 10.1016/j.pss.2021.105185. URL <https://linkinghub.elsevier.com/retrieve/pii/S0032063321000246>.
- E. S. Douglas, K. Tracy, and Z. Manchester. Practical limits on Nanosatellite Telescope Pointing: The Impact of Disturbances and Photon Noise. *Frontiers in Astronomy and Space Sciences*, 8:676252, Aug. 2021. ISSN 2296-987X. doi: 10.3389/fspas.2021.676252. URL <http://arxiv.org/abs/2112.12835>. arXiv:2112.12835 [astro-ph].
- EnduroSat. EnduroSat 16U CubeSat Platform, 2025. URL <https://www.endurosat.com/products/16u-platform/>.
- ESA-TEC. ENGINEERING GUIDELINES FOR ESA IOD CUBESAT PROJECTS, 2025.
- ESA-TECSAA. 3DOF Attitude Control Air Bearing Facility. Statement of Work ESA-TECSAA-SOW-2022-002591. 2022.
- European Cooperation for Space Standardization. Space Engineering: Testing. Space Engineering Standard ECSS-E-ST-10-03C Rev. 1, European Space Agency, May 2022. URL <https://ecss.nl/standard/ecss-e-st-10-03c-rev-1-testing-31-may-2022/>.
- R. Fullmer, G. Peterson, W. Holmans, J. Smith, J. Nottingham, S. Anderson, T. Olsen, and F. Redd. The development of a small satellite attitude control simulator. 1992.
- D. Gallardo, R. Bevilacqua, and R. Rasmussen. Advances on a 6 Degrees of Freedom Testbed for Autonomous Satellites Operations. In *AIAA Guidance, Navigation, and Control Conference*, Portland, Oregon, 2011. American Institute of Aeronautics and Astronautics. ISBN 978-1-60086-952-5. doi: 10.2514/6.2011-6591. URL <https://arc.aiaa.org/doi/10.2514/6.2011-6591>.
- I. Gavrilovich. *Development of a robotic system for CubeSat Attitude Determination and Control System ground tests*. PhD Thesis, Université Montpellier, 2016.

- I. Gavrilovich, S. Krut, M. Gouttefarde, F. Pierrot, and L. Dusseau. Robotic test bench for CubeSat ground testing: Concept and satellite dynamic parameter identification. In *2015 IEEE/RSJ International Conference on Intelligent Robots and Systems (IROS)*, pages 5447–5453, Hamburg, Germany, Sept. 2015. IEEE. ISBN 978-1-4799-9994-1. doi: 10.1109/IROS.2015.7354148. URL <http://ieeexplore.ieee.org/document/7354148/>.
- I. Gavrilovich, S. Krut, M. Gouttefarde, F. Pierrot, and L. Dusseau. Innovative approach to use air bearings in cubesat ground tests. In *CubeSat Workshop*, 2016.
- B. S. T. GmbH. NanoSat Air Bearing 2nd Gen, 2024. URL <https://satsearch.co/products/berlin-space-tech-nanosat-air-bearing-2nd-generation>.
- A. Golkar and A. Salado. Definition of New Space—Expert Survey Results and Key Technology Trends. *IEEE Journal on Miniaturization for Air and Space Systems*, 2(1):2–9, Mar. 2021. ISSN 2576-3164. doi: 10.1109/JMASS.2020.3045851. URL <https://ieeexplore.ieee.org/document/9298818/>.
- GomSpace. CubeSat Space Protocol (CSP) Network-Layer delivery protocol for CubeSats and embedded system. 2011. URL <https://bytebucket.org/bbruner0/albertasat-on-board-computer/wiki/1.%20Resources/1.1.%20DataSheets/CSP/GS-CSP-1.1.pdf?rev=316ebd49bed49fdbb1d74efdeab74430e7cc726a>.
- M. Innocenti. Chapter 7: mu Analysis and Synthesis, 2021. URL https://people.unipi.it/mario_innocenti/wp-content/uploads/sites/256/2021/12/7_CSI_2021_MU.pdf. Published: Lecture notes for Control Systems II (CSI).
- ISO. Information Technology—Open Systems Interconnection—Basic Reference Model: The Basic Model. International Standard ISO/IEC 7498-1:1994, International Organization for Standardization, Geneva, Switzerland, 1994. URL <https://www.iso.org/standard/20269.html>. Edition: 2.
- ISO. Space systems – Cube satellites (CubeSats). ISO Standard ISO 17770:2017 (Ref. 60496), International Organization for Standardization, Geneva, Switzer-

- land, June 2017. URL <https://www.iso.org/standard/60496.html>. Edition: 1.
- D. S. Ivanov, T. A. Ivanova, N. A. Ivlev, M. Y. Ovchinnikov, and D. S. Roldugin. Estimation of an Inertia Tensor and Automatic Balancing of a Microsatellite Mockup on an Air-Bearing Testbed. *Journal of Computer and Systems Sciences International*, 60(2):315–332, Mar. 2021. ISSN 1064-2307, 1555-6530. doi: 10.1134/S1064230721020088. URL <https://link.springer.com/10.1134/S1064230721020088>.
- K. Janschek, V. Tchernykh, and S. Dyblenko. Performance analysis of optomechatronic image stabilization for a compact space camera. *Control Engineering Practice*, 15(3):333–347, Mar. 2007. ISSN 09670661. doi: 10.1016/j.conengprac.2006.02.010. URL <https://linkinghub.elsevier.com/retrieve/pii/S0967066106000281>.
- Joan, Abyz. pigpio: A C library for Raspberry Pi GPIO control, 2023. URL <https://abyz.me.uk/rpi/pigpio/index.html>.
- H. Jones. The recent large reduction in space launch cost. 48th International Conference on Environmental Systems, 2018.
- D. Jung and P. Tsiotras. A 3-DoF Experimental Test-Bed for Integrated Attitude Dynamics and Control Research. In *AIAA Guidance, Navigation, and Control Conference and Exhibit*, Austin, Texas, Aug. 2003. American Institute of Aeronautics and Astronautics. ISBN 978-1-62410-090-1. doi: 10.2514/6.2003-5331. URL <https://arc.aiaa.org/doi/10.2514/6.2003-5331>.
- M. Kabganian, M. Nabipour, and F. F. Saberi. Design and implementation of attitude control algorithm of a satellite on a three-axis gimbal simulator. *Proceedings of the Institution of Mechanical Engineers, Part G: Journal of Aerospace Engineering*, 229(1):72–86, Jan. 2015. ISSN 0954-4100, 2041-3025. doi: 10.1177/0954410014526380. URL <https://journals.sagepub.com/doi/10.1177/0954410014526380>.
- J. J. Kim and B. N. Agrawal. Automatic Mass Balancing of Air-Bearing-Based Three-Axis Rotational Spacecraft Simulator. *Journal of Guidance, Control, and*

- Dynamics*, 32(3):1005–1017, May 2009. ISSN 0731-5090, 1533-3884. doi: 10.2514/1.34437. URL <https://arc.aiaa.org/doi/10.2514/1.34437>.
- E. Kulu. CubeSats & Nanosatellites - 2024 Statistics, Forecast and Reliability. In *Proceedings of the 75th International Astronautical Congress (IAC 2024)*, pages IAC–24.B4.6A.13, Milan, Italy, Oct. 2024. URL https://www.nanosats.eu/assets/CubeSats-Nanosatellites-2024_ErikKulu_IAC2024.pdf.
- E. Kulu. Nanosats Database, 2025. URL <https://www.nanosats.eu>.
- T. H. Kwan, K. M. B. Lee, J. Yan, and X. Wu. An air bearing table for satellite attitude control simulation. In *2015 IEEE 10th Conference on Industrial Electronics and Applications (ICIEA)*, pages 1420–1425, Auckland, New Zealand, June 2015. IEEE. ISBN 978-1-4799-8389-6. doi: 10.1109/ICIEA.2015.7334330. URL <http://ieeexplore.ieee.org/document/7334330/>.
- G. Lavezzi, N. J. Stang, and M. Ciarcià. START: A Satellite Three Axis Rotation Testbed. *Micromachines*, 13(2):165, Jan. 2022. ISSN 2072-666X. doi: 10.3390/mi13020165. URL <https://www.mdpi.com/2072-666X/13/2/165>.
- M. P. Le. *Micro-disturbances in reaction wheels*. Phd Thesis 1 (Research TU/e / Graduation TU/e), Applied Physics and Science Education, Mar. 2017. ISBN: 978-90-386-4221-5.
- J. Li, M. Post, T. Wright, and R. Lee. Design of Attitude Control Systems for CubeSat-Class Nanosatellite. *Journal of Control Science and Engineering*, 2013: 1–15, 2013. ISSN 1687-5249, 1687-5257. doi: 10.1155/2013/657182. URL <http://www.hindawi.com/journals/jcse/2013/657182/>.
- libcsp authors. Cubesat Space Protocol Authors. 2023a. URL <https://github.com/libcsp/libcsp/blob/develop/AUTHORS>.
- libcsp authors. The Cubesat Space Protocol (CSP). 2023b. URL <https://github.com/libcsp/libcsp>.
- Libre Space Foundation. Open Space Data Link Protocols (OSDLP), 2023. URL <https://gitlab.com/librespacefoundation/osdlp>.

- C. Liu, X. Jing, S. Daley, and F. Li. Recent advances in micro-vibration isolation. *Mechanical Systems and Signal Processing*, 56-57:55–80, May 2015. ISSN 08883270. doi: 10.1016/j.ymssp.2014.10.007. URL <https://linkinghub.elsevier.com/retrieve/pii/S0888327014004014>.
- A. McCafferty-Leroux, A. Newton, and S. A. Gadsden. Parameter estimation and control of an automatic balancing system for CubeSat research and applications. In K. D. Pham and G. Chen, editors, *Sensors and Systems for Space Applications XVII*, page 23, National Harbor, United States, June 2024. SPIE. ISBN 978-1-5106-7442-4 978-1-5106-7443-1. doi: 10.1117/12.3013732. URL <https://www.spiedigitallibrary.org/conference-proceedings-of-spie/13062/3013732/Parameter-estimation-and-control-of-an-automatic-balancing-system-for/10.1117/12.3013732.full>.
- D. M. Meissner. *A three degrees of freedom test-bed for nanosatellite and Cubesat attitude dynamics, determination, and control*. PhD Thesis, Monterey, California. Naval Postgraduate School, 2009.
- Y. Mellier, A. Abdurrouf, J. A. Barroso, A. Achúcarro, J. Adamek, R. Adam, G. Addison, N. Aghanim, M. Agüena, V. Ajani, and others. Euclid. I. Overview of the Euclid mission. *Astronomy & Astrophysics*, 2024. Publisher: EDP sciences.
- C. Mittelsteadt and E. Mehiel. Cal Poly Spacecraft Attitude Dynamics Simulator: CP/SADS. In *AIAA Guidance, Navigation and Control Conference and Exhibit*, Hilton Head, South Carolina, Aug. 2007. American Institute of Aeronautics and Astronautics. ISBN 978-1-62410-015-4. doi: 10.2514/6.2007-6443. URL <https://arc.aiaa.org/doi/10.2514/6.2007-6443>.
- D. Modenini, A. Bahu, G. Curzi, and A. Togni. A Dynamic Testbed for Nanosatellites Attitude Verification. *Aerospace*, 7(3):31, 2020. ISSN 2226-4310. doi: 10.3390/aerospace7030031. URL <https://www.mdpi.com/2226-4310/7/3/31>.
- NASA. CubeSat 101: Basic Concepts and Processes for First-Time CubeSat Developers (pdf), 2017. URL https://www3.nasa.gov/sites/default/files/atoms/files/nasa_csli_cubesat_101_508.pdf.

- NASA. What are SmallSats and CubeSats?, 2024. URL <https://www.nasa.gov/what-are-smallsats-and-cubesats/>.
- NASA. State-of-the-Art of Small Spacecraft Technology (SST SoA), 2025. URL <https://www.nasa.gov/smallsat-institute/sst-soa/>.
- A. Nicolai, C.-M. Mora, R. Bretfeld, and H. Jahn. Scaling down the attitude control hardware-in-the-loop test facility for CubeSats and introduction of the auto-calibration. In M. Petrozzi-Illstad, editor, *Small Satellites Systems and Services Symposium (4S 2024)*, page 95, Palma de Mallorca, Spain, Mar. 2025. SPIE. ISBN 978-1-5106-8894-0 978-1-5106-8895-7. doi: 10.1117/12.3062474. URL <https://www.spiedigitallibrary.org/conference-proceedings-of-spie/13546/3062474/Scaling-down-the-attitude-control-hardware-in-the-loop-test/10.1117/12.3062474.full>.
- K. O'Donnell and G. Richardson. Small Satellite Trending & Reliability 2009-2018. In *Proceedings of the 34th Annual Small Satellite Conference*, SSC20, Logan, Utah, 2020. Utah State University. Issue: S2-01.
- OECD. *The Space Economy in Figures: Responding to Global Challenges*. OECD, Dec. 2023. ISBN 978-92-64-88307-9 978-92-64-62479-5 978-92-64-47919-7. doi: 10.1787/fa5494aa-en. URL https://www.oecd.org/en/publications/the-space-economy-in-figures_fa5494aa-en.html.
- OECD. Space economy investment trends: OECD insights for attracting high-quality funding. OECD Science, Technology and Industry Policy Papers 166, Apr. 2024. URL https://www.oecd.org/en/publications/space-economy-investment-trends_9ae9a28d-en.html. Series: OECD Science, Technology and Industry Policy Papers Volume: 166.
- K. Ogata. *Modern Control Engineering*. Instrumentation and controls series. Prentice Hall, 2010. ISBN 978-0-13-615673-4. URL <https://books.google.nl/books?id=Wu5GpNAelzkC>.
- J. A. Olsen. Attitude Determination and Control System Testbed for Hardware and Software Testing and Verification for Small Satellites. Master's thesis, NTNU, 2021.

- D. Paikowsky. What Is New Space? The Changing Ecosystem of Global Space Activity. *New Space*, 5(2):84–88, June 2017. ISSN 2168-0256, 2168-0264. doi: 10.1089/space.2016.0027. URL <https://www.liebertpub.com/doi/10.1089/space.2016.0027>.
- M. A. Peck, L. Miller, A. R. Cavender, M. Gonzalez, and T. Hintz. An airbearing-based testbed for momentum control systems and spacecraft line of sight. *Advances in the Astronautical Sciences*, 114:427–446, 2003. Publisher: Citeseer.
- L. Peri, J. De Maeyer, W. De Munter, M. Keunen, and D. Vandepitte. Development of a High-Precision Pointing Platform for the CubeSpec Mission. In *AIAA SCITECH 2025 Forum*, Orlando, FL, Jan. 2025. American Institute of Aeronautics and Astronautics. ISBN 978-1-62410-723-8. doi: 10.2514/6.2025-2318. URL <https://arc.aiaa.org/doi/10.2514/6.2025-2318>.
- T. Petermann, E. Jäger, L. Elsner, D. Pearson, G. Dietl, and K. Schilling. A Distributed Hardware-in-the-Loop Testbed for Attitude Control of Small Communication Satellites. In *2025 IEEE Space Hardware and Radio Conference (SHaRC)*, pages 8–11, San Juan, PR, USA, Jan. 2025. IEEE. ISBN 979-8-3315-0736-7. doi: 10.1109/SHaRC63955.2025.10908960. URL <https://ieeexplore.ieee.org/document/10908960/>.
- Philip Linden, PlanetFollow, Nicolas Smith, Planet, and Thomas Rohrbach, Planet. Automatic Optical Image Stabilization System Calibration, Validation, and Performance for the SkySat Constellation. 2021.
- R. Pintelon and J. Schoukens. *System identification: a frequency domain approach*. John Wiley & Sons, 2nd edition, 2012.
- R. Pintelon, K. Barb'e, G. Vandersteen, and J. Schoukens. Improved (non-)parametric identification of dynamic systems excited by periodic signals. *Mechanical Systems and Signal Processing*, 25(7):2683–2704, 2011. doi: 10.1016/j.ymssp.2011.03.003.
- A. Poghosyan and A. Golkar. CubeSat evolution: Analyzing CubeSat capabilities for conducting science missions. *Progress in Aerospace Sciences*, 88:59–83, Jan. 2017a. ISSN 03760421. doi: 10.1016/j.paerosci.2016.11.002. URL <https://linkinghub.elsevier.com/retrieve/pii/S0376042116300951>.

- A. Poghosyan and A. Golkar. CubeSat evolution: Analyzing CubeSat capabilities for conducting science missions. *Progress in Aerospace Sciences*, 88:59–83, Jan. 2017b. ISSN 03760421. doi: 10.1016/j.paerosci.2016.11.002. URL <https://linkinghub.elsevier.com/retrieve/pii/S0376042116300951>.
- H. C. Polat, J. Virgili-Llop, and M. Romano. Survey, Statistical Analysis and Classification of Launched CubeSat Missions with Emphasis on the Attitude Control Method. *Journal of Small Satellites*, 5:513–530, Oct. 2016. URL <https://ui.adsabs.harvard.edu/abs/2016JSSat...5..513P>. ADS Bibcode: 2016JS-Sat...5..513P.
- J. Prado, G. Bisiacchi, L. Reyes, E. Vicente, F. Contreras, M. Mesinas, and A. Juárez. Three-axis air-bearing based platform for small satellite attitude determination and control simulation. *Journal of Applied Research and Technology*, 3(03), Dec. 2005. ISSN 2448-6736, 1665-6423. doi: 10.22201/icat.16656423.2005.3.03.563. URL <http://www.jart.icat.unam.mx/index.php/jart/article/view/563>.
- V. Preda. *Robust microvibration control and worst-case analysis for high pointing stability space missions*. PhD Thesis, Université de Bordeaux, 2017.
- V. Preda, J. Cieslak, D. Henry, S. Bennani, and A. Falcoz. Robust microvibration mitigation and pointing performance analysis for high stability spacecraft. *International Journal of Robust and Nonlinear Control*, 28(18):5688–5716, Dec. 2018. ISSN 1049-8923, 1099-1239. doi: 10.1002/rnc.4338. URL <https://onlinelibrary.wiley.com/doi/10.1002/rnc.4338>.
- M. Prinkey. CubeSat Attitude Control Testbed Design: Merritt 4-Coil per axis Helmholtz Cage and Spherical Air Bearing. In *AIAA Guidance, Navigation, and Control (GNC) Conference*, Boston, MA, Aug. 2013. American Institute of Aeronautics and Astronautics. ISBN 978-1-62410-224-0. doi: 10.2514/6.2013-4942. URL <https://arc.aiaa.org/doi/10.2514/6.2013-4942>.
- A. Probe, V. Guimaraes Goecks, and J. Hurtado. Low Cost Satellite Attitude Hardware Test Bed. In *2016 ASEE Annual Conference & Exposition Proceedings*, page 25582, New Orleans, Louisiana, June 2016. ASEE Conferences. doi: 10.18260/p.25582. URL <http://peer.asee.org/25582>.

- T. Prusti, J. De Bruijne, A. G. Brown, A. Vallenari, C. Babusiaux, C. Bailer-Jones, U. Bastian, M. Biermann, D. W. Evans, L. Eyer, and others. The gaia mission. *Astronomy & astrophysics*, 595:A1, 2016. Publisher: EDP sciences.
- J. G. Puetite and A. M. Werth. Demand-assigned service for the Intelsat global network. *IEEE Spectrum*, 8(1):59–69, Jan. 1971. ISSN 0018-9235, 1939-9340. doi: 10.1109/MSPEC.1971.6501064. URL <https://ieeexplore.ieee.org/document/6501064/>.
- P. Puig-suari, C. Turner, and R. Twiggs. CubeSat: The Development and Launch Support Infrastructure for Eighteen Different Satellite Customers on One Launch. Jan. 2001.
- M. Rivandi, M. Mirshams, and M. Zarourati. Design and Implementation of a Balance System for the CubeSat Attitude Determination and Control Tabletop Simulator. *Journal of Space Science and Technology*, 16(1):75–88, May 2023. ISSN 2423-4516. doi: 10.30699/jsst.2023.1426. URL https://www.jsstpub.com/article_171531.html.
- O. Ruf, M. Von Arnim, F. Kempf, R. Haber, L. Elsner, J. Dauner, S. Dombrowski, A. Kramer, and K. Schilling. Advanced test environment for automated attitude control testing of fully integrated CubeSats on system level. *CEAS Space Journal*, 16(4):491–510, July 2024. ISSN 1868-2502, 1868-2510. doi: 10.1007/s12567-023-00523-x. URL <https://link.springer.com/10.1007/s12567-023-00523-x>.
- N. Saeed, A. Elzanaty, H. Almorad, H. Dahrouj, T. Y. Al-Naffouri, and M.-S. Alouini. CubeSat Communications: Recent Advances and Future Challenges. *IEEE Communications Surveys & Tutorials*, 22(3):1839–1862, 2020. ISSN 1553-877X, 2373-745X. doi: 10.1109/COMST.2020.2990499. URL <https://ieeexplore.ieee.org/document/9079470/>.
- J. Schoolcraft, A. Klesh, and T. Werne. MarCO: Interplanetary Mission Development on a CubeSat Scale. In C. Cruzen, M. Schmidhuber, Y. H. Lee, and B. Kim, editors, *Space Operations: Contributions from the Global Community*, pages 221–231. Springer International Publishing, Cham, 2017. ISBN 978-3-319-51940-1 978-3-319-51941-8. doi: 10.1007/978-3-319-51941-8_10. URL http://link.springer.com/10.1007/978-3-319-51941-8_10.

- J. Schoukens, G. Vandersteen, R. Pintelon, Z. Emedi, and Y. Rolain. Bounding the Polynomial Approximation Errors of Frequency Response Functions. *IEEE Transactions on Instrumentation and Measurement*, 62(5):1346–1353, May 2013. doi: 10.1109/TIM.2012.2232451.
- J. Schoukens, K. Godfrey, and M. Schoukens. Nonparametric data-driven modeling of linear systems: Estimating the frequency response and impulse response function. *IEEE Control Systems Magazine*, 38(4):49–88, 2018.
- J. L. Schwartz, M. A. Peck, and C. D. Hall. Historical Review of Air-Bearing Spacecraft Simulators. *Journal of Guidance, Control, and Dynamics*, 26(4):513–522, July 2003. ISSN 0731-5090, 1533-3884. doi: 10.2514/2.5085. URL <https://arc.aiaa.org/doi/10.2514/2.5085>.
- D. Selva and D. Krejci. A survey and assessment of the capabilities of Cubesats for Earth observation. *Acta Astronautica*, 74:50–68, 2012. ISSN 00945765. doi: 10.1016/j.actaastro.2011.12.014. URL <https://linkinghub.elsevier.com/retrieve/pii/S0094576511003742>.
- G. Sharifi, M. Mirshams, and H. Shahmohamadi Ousaloo. Mass properties identification and automatic mass balancing system for satellite attitude dynamics simulator. *Proceedings of the Institution of Mechanical Engineers, Part G: Journal of Aerospace Engineering*, 233(3):896–907, Mar. 2019. ISSN 0954-4100, 2041-3025. doi: 10.1177/0954410017742932. URL <https://journals.sagepub.com/doi/10.1177/0954410017742932>.
- R. C. D. Silva, F. C. Guimarães, J. V. L. D. Loiola, R. A. Borges, S. Battistini, and C. Cappelletti. Tabletop Testbed for Attitude Determination and Control of Nanosatellites. *Journal of Aerospace Engineering*, 32(1):04018122, Jan. 2019. ISSN 0893-1321, 1943-5525. doi: 10.1061/(ASCE)AS.1943-5525.0000952. URL <https://ascelibrary.org/doi/10.1061/%28ASCE%29AS.1943-5525.0000952>.
- C. P. SLO. CubeSat Design Specification Rev. 14.1. *The CubeSat program*, 2025.
- M. W. Smith, A. Donner, M. Knapp, C. M. Pong, C. H. Smith, J. Luu, P. D. Pasquale, R. L. Bocchino, B. Campuzano, J. S. Loveland, C. Collery, A. Babus-

- cia, M. White, J. Krajewski, and S. Seager. On-Orbit Results and Lessons Learned from the ASTERIA Space Telescope Mission. 2018.
- Sputnix. Dynamic simulator for spacecraft ADCS tests SXL-SCOE-ADCS, 2020. URL <https://sputnix.ru/tp1/docs/ADCS%20test%20bench%20ENG-web.pdf>.
- A. C. Stickler and K. Alfriend. Elementary Magnetic Attitude Control System. *Journal of Spacecraft and Rockets*, 13(5):282–287, May 1976. ISSN 0022-4650, 1533-6794. doi: 10.2514/3.57089. URL <https://arc.aiaa.org/doi/10.2514/3.57089>.
- M. Stromecki and A. Bani Younes. Spacecraft attitude testbed. *Advances in Space Research*, 74(5):2239–2269, Sept. 2024. ISSN 02731177. doi: 10.1016/j.asr.2024.05.069. URL <https://linkinghub.elsevier.com/retrieve/pii/S0273117724005350>.
- Swartwout. CubeSat Mission Success: Are We Getting Better?, 2019. URL https://drive.google.com/file/d/13Ap7bpefBqVa02psjm_6GoFryDTwvFIS/view.
- M. Swartwout. The First One Hundred CubeSats: A Statistical Look. *Journal of Small Satellites*, 2:213–233, Dec. 2013. URL <https://ui.adsabs.harvard.edu/abs/2013JSSat...2..213S>. ADS Bibcode: 2013JSSat...2..213S.
- M. N. Sweeting. Modern Small Satellites-Changing the Economics of Space. *Proceedings of the IEEE*, 106(3):343–361, Mar. 2018. ISSN 0018-9219, 1558-2256. doi: 10.1109/JPROC.2018.2806218. URL <https://ieeexplore.ieee.org/document/8303876/>.
- A. Tavakoli, A. Faghihinia, and A. Kalhor. An innovative test bed for verification of attitude control system. *IEEE Aerospace and Electronic Systems Magazine*, 32(6):16–22, June 2017. ISSN 0885-8985. doi: 10.1109/MAES.2017.150198. URL <http://ieeexplore.ieee.org/document/7990415/>.
- The MathWorks Inc. Balanced stochastic model truncation (BST) via Schur method bstmr, 2023. URL <https://www.mathworks.com/help/robust/ref/dynamicsystem.bstmr.html>. Publisher: The MathWorks Inc.

- The MathWorks Inc. fitmagfrd: Fit magnitude data with stable and minimum phase transfer function, 2025a. URL <https://www.mathworks.com/help/robust/ref/frd.fitmagfrd.html>. Publisher: The MathWorks Inc.
- The MathWorks Inc. H-inf Synthesis, 2025b. URL <https://www.mathworks.com/help/robust/controller-synthesis.html>. Publisher: The MathWorks Inc.
- The MathWorks Inc. musyn: Robust controller synthesis for uncertain systems, 2025c. URL <https://www.mathworks.com/help/robust/ref/uss.musyn.html>. Publisher: The MathWorks Inc.
- The MathWorks Inc. Robust H-inf performance optimized by musyn, 2025d. URL <https://www.mathworks.com/help/robust/ref/dynamicsystem.musynperf.html>. Publisher: The MathWorks Inc.
- The MathWorks Inc. Robust stability of uncertain system, 2025e. URL <https://www.mathworks.com/help/robust/ref/dynamicsystem.robstab.html>. Publisher: The MathWorks Inc.
- The MathWorks Inc. systune: Tune control system parameters to meet design requirements, 2025f. URL <https://www.mathworks.com/help/control/ref/dynamicsystem.systune.html>. Publisher: The MathWorks Inc.
- The MathWorks Inc. ucover: Compute normalized coprime factor uncertainty bounds, 2025g. URL <https://www.mathworks.com/help/robust/ref/dynamicsystem.ucover.html>. Publisher: The MathWorks Inc.
- M. L. Tibbs. Design and Test of an Attitude Determination and Control System for a 6U CubeSat using AFIT's CubeSat Testbed. 2015.
- C. S. G. E. U. o. T. TU/e. Active Vibration Isolation Platform for Adaptable Payloads, Final Report, 2024.
- US Congress. Commercial Space Launch Act : P.L. 98-575, (H.R. 3942), 98 Stat. 3055, October 30, 1984, 1984. URL <https://lawcat.berkeley.edu/record/1232759>.
- T. Ustrzycki. Spherical Air Bearing testbed for nanosatellite attitude control development. Jan. 2011.

- C. Venturini, B. Braun, D. Hinkley, and G. Berg. Improving mission success of CubeSats. 2018.
- T. Villela, C. A. Costa, A. M. Brandão, F. T. Bueno, and R. Leonardi. Towards the Thousandth CubeSat: A Statistical Overview. *International Journal of Aerospace Engineering*, 2019:1–13, Jan. 2019a. ISSN 1687-5966, 1687-5974. doi: 10.1155/2019/5063145. URL <https://www.hindawi.com/journals/ijae/2019/5063145/>.
- T. Villela, C. A. Costa, A. M. Brandão, F. T. Bueno, and R. Leonardi. Towards the Thousandth CubeSat: A Statistical Overview. *International Journal of Aerospace Engineering*, 2019:1–13, Jan. 2019b. ISSN 1687-5966, 1687-5974. doi: 10.1155/2019/5063145. URL <https://www.hindawi.com/journals/ijae/2019/5063145/>.
- vlab. Free Vibration of a Cantilever Beam (Continuous System), 2011. URL vlab.amrita.edu/?sub=3&brch=175&sim=1080&cnt=1.
- B. Walleshauser, N. Boerger, and M. C. Bazzocchi. CubeSat Attitude Control Emulation Platform with Three-Axis Gimbal and Helmholtz Cage. *IEEE Aerospace and Electronic Systems Magazine*, pages 1–10, 2025. ISSN 0885-8985, 1557-959X. doi: 10.1109/MAES.2025.3566653. URL <https://ieeexplore.ieee.org/document/11014620/>.
- J. R. Wertz, D. F. Everett, and J. J. Puschell, editors. *Space mission engineering: the new SMAD*. Number v. 28 in Space technology library. Microcosm Press : Sold and distributed worldwide by Microcosm Astronautics Books, Hawthorne, CA, 2011. ISBN 978-1-881883-16-6 978-1-881883-15-9.
- K. Woellert, P. Ehrenfreund, A. J. Ricco, and H. Hertzfeld. Cubesats: Cost-effective science and technology platforms for emerging and developing nations. *Advances in Space Research*, 47(4):663–684, Feb. 2011. ISSN 02731177. doi: 10.1016/j.asr.2010.10.009. URL <https://linkinghub.elsevier.com/retrieve/pii/S0273117710006836>.
- A. T. Wolosik. *Advancements in the design and development of Cubesat attitude determination and control testing at the Virginia Tech space systems simulation laboratory*. PhD Thesis, Virginia Tech, 2018.

- X. Xia, G. Sun, K. Zhang, S. Wu, T. Wang, L. Xia, and S. Liu. NanoSats/CubeSats ADCS survey. In *2017 29th Chinese Control And Decision Conference (CCDC)*, pages 5151–5158, Chongqing, China, May 2017. IEEE. ISBN 978-1-5090-4657-7. doi: 10.1109/CCDC.2017.7979410. URL <http://ieeexplore.ieee.org/document/7979410/>.
- Z. Xu, N. Qi, and Y. Chen. Parameter estimation of a three-axis spacecraft simulator using recursive least-squares approach with tracking differentiator and Extended Kalman Filter. *Acta Astronautica*, 117:254–262, Dec. 2015. ISSN 00945765. doi: 10.1016/j.actaastro.2015.08.010. URL <https://linkinghub.elsevier.com/retrieve/pii/S0094576515003264>.
- P. R. Yanyachi, A. Mamani-Saico, X. Wang, and B. Espinoza-García. Development of an Air-Bearing Testbed for Nanosatellite Attitude Determination and Control Systems. *IEEE Access*, 12:168864–168876, 2024. ISSN 2169-3536. doi: 10.1109/ACCESS.2024.3497722. URL <https://ieeexplore.ieee.org/document/10752510/>.
- yashi. Cubesat Space Protocol changelog. 2023. URL <https://github.com/libcsp/libcsp/blob/develop/CHANGELOG>.
- K. Zhou and J. C. Doyle. *Essentials of Robust Control*. Prentice Hall, 1999. ISBN 0-13-525833-2.