

Development of a Biofidelic Rollover Dummy-Part II: Validation of the Kinematic Response of THOR Multi-Body and Finite Element Models Relative to Response of the Physical THOR Dummy under Laboratory Rollover Conditions

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ABSTRACT

While over 30% of US occupant fatalities occur in rollover crashes, no dummy has been developed for such a condition. Currently, an efficient, cost-effective methodology is being implemented to develop a biofidelic rollover dummy. Instead of designing a rollover dummy from scratch, this methodology identifies a baseline dummy and modifies it to improve its response in a rollover crash. Using computational models of the baseline dummy, including both multibody (MB) and finite element (FE) models, the dummy's structure is continually modified until its response is aligned (using BioRank/CORA metric) with biofidelity targets. A previous study (Part I) identified the THOR dummy as a suitable baseline dummy by comparing the kinematic responses of six existing dummies with PMHS response corridors through laboratory rollover testing. In this study (Part II), the whole-body kinematic responses of the THOR MB and FE models were validated with responses of the physical THOR dummy in experiments that simulated rollover conditions. This step is necessary to ensure accuracy of the computer-aided engineering dummy design, thereafter improving confidence in the proposed rollover dummy design modifications. In addition, to ensure the robustness of the model validation, the sensitivities of the THOR dummy computational model responses to parameters with uncertainty in the experiment were assessed, including seatbelt pretension, friction, and dummy seating posture. In summary, both the THOR MB and FE model responses matched well with its physical counterpart. Future studies (Part III) will focus on using these validated dummy models for rollover dummy design modification and evaluation.

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INTRODUCTION

While rollover crashes only accounted for 2.2% of the total crashes in 2012, they caused 34.6% of the total occupant traffic fatalities [1]. Currently, the federal motor vehicle safety standard (FMVSS) No. 216 regulation is the only federal regulation that governs vehicle rollover crashworthiness in the US. The FMVSS 216 regulation rates vehicle rollover crashworthiness by a value called roof strength-to-weight ratio through a quasi-static test procedure that measures the force required to push a metal plate into the roof within 5 inches displacement at a constant quasi-static rate and loading angle [2].

However, the FMVSS 216 test procedure has been criticized for being a quasi-static test that does not accurately represent vehicle structural responses to real-world dynamic rollover [3,4,5]. The loading vector in the FMVSS 216 test is constant, and it neither varies with time, as it would for a rolling vehicle interacting with the ground, nor is it necessarily the worst loading case for a given roof structure [6, 7]. In addition, unlike other FMVSS vehicle crashworthiness regulations, the FMVSS 216 regulation does not use a crash test dummy to predict occupant injury risk for vehicle crashworthiness evaluation.

While NHTSA has acknowledged that their ultimate goal is to pursue a dynamic rollover testing regulation that uses a crash dummy for injury prediction and crashworthiness rating, there are several hurdles that prevent the implementation of a dynamic rollover testing procedure as the federal regulation, including: 1) the inadequacy of repeatability of existing dynamic rollover fixture and testing procedure; 2) the lack of an occupant injury criteria under rollover conditions; 3) the lack of a dummy available that has demonstrated biofidelity in rollover [2].

While no federal regulation has yet mandated a dynamic rollover testing procedure, a number of dynamic rollover test methods do exist and have been used for occupant responses prediction and counter-measure design. These fixtures include the Controlled Rollover Impact System (CRIS) [8], the Dynamic Rollover Testing System [9], the Deceleration Rollover Sled (DRS) [10], etc. Due to the lack of a dummy designed specifically for rollover crashes, such dynamic rollover tests have often been performed with dummies developed for other crash scenarios [3, 11,12,13,14]. The most commonly used dummy for such tests is the Hybrid III dummy. However, Moffatt et al. have indicated that the spine of the Hybrid III dummy is too stiff

for rollover crash conditions [15, 16]. Effective passive countermeasure design for a specific crash type requires a thorough understanding of occupant responses under the investigated conditions. Using a crash test dummy developed for non-rollover conditions for rollover occupant response prediction could result in ineffective occupant protection system development. Therefore, developing a biofidelic rollover crash dummy is an urgent task for improving vehicle rollover crashworthiness.

This study is part of a larger effort that intends to develop a biofidelic dummy for rollover crash testing. An efficient and cost-effective methodology is proposed and employed, after a review of the advantages and disadvantages of the existing dummy development approach. This methodology features: 1) Instead of developing a rollover dummy from scratch, which is time-consuming and costly, a baseline dummy is identified and its responses are improved for rollover crashes, in a way similar to the development of the Polar pedestrian dummy [17, 18]. This approach is attractive, because existing dummies might already possess some features that are necessary for a biofidelic response of a rollover crash dummy. 2) The role of computer aided engineering (CAE) in previous dummy development was limited. Nevertheless the increasing trend of its use was observed: from the Hybrid III [19] to the Polar [17, 18] and the BioRID dummy [20, 21]. Thanks to the recent rapid advances in both computational power and crash simulation technology, a primarily CAE-driven dummy design approach (complemented with experimental testing) became possible for a more efficient and cost-effective rollover dummy design. Both multi-body (MB) modeling and finite element (FE) modeling will be employed in the CAE dummy design. The MB modeling will focus on evaluating initial design concepts to allow a fast design turn-around. This is because a rollover crash is a long duration event that could last for seconds, which presents a computational challenge for a pure FE dummy design optimization process. The FE modeling will be used primarily in the later design phase. 3) As in the development of the WorldSID dummy [22], an objective rating method will also be used in the rollover dummy development to allow a more quantitative and objective approach to dummy performance evaluation and optimization relative to the biofidelic response targets.

Lessley et al. characterized and constructed the whole-body three dimensional kinematic response corridors of restrained Post Mortem Human Surrogate (PMHS) in controlled laboratory rollover tests [23]. To identify a baseline dummy for rollover dummy development, Zhang et al. performed laboratory rollover tests with six existing dummies (Hybrid III, Hybrid III Pedestrian, Hybrid III with Pedestrian Pelvis, WorldSID, Polar II and THOR) in the same conditions as the PMHS tests conducted by Lessley et al., and concluded that the kinematic response differences among the dummies was insignificant when compared with the differences observed between the dummies and PMHS responses [24]. Since all six dummies evaluated exhibited similar kinematic responses in rollover crash tests, the THOR dummy was chosen as the baseline dummy, for other factors including: 1) the availability of both MB and FE THOR dummy models for rollover dummy CAE design; and 2) its realistic ribcage geometry and additional thoracic spinal joint, which might provide a good starting point for modifying the spine design of the rollover dummy.

However, before the THOR dummy computational models are used for rollover dummy CAE design, the responses of the models need to be validated with the physical THOR dummy in rollover conditions. This validation provides an appropriate starting point for the accurate CAE dummy design work, and ensures that no model errors or deficiencies are carried into the next phase of the dummy design.

The primary goal of this study was to validate the kinematic responses of the THOR MB and FE models with the responses of the physical THOR dummy in rollover experiments presented previously [24]. The secondary goal was to provide a benchmark of the biofidelity level of the THOR dummy (both the physical dummy and numerical models) relative to the PMHS response corridors under the same rollover conditions [23].

METHODOLOGY

THOR FE Model and Update

The THOR FE model was developed originally to represent the THOR dummy alpha version [25], and was then continuously updated and improved by different researchers [26, 27], following the design update of the THOR dummy. The THOR FE model (v 2.0.6) used in the current study represented the THOR Mod-Kit hardware dummy (Figure 1b).



Figure 1. (a) THOR dummy; (b) THOR FE model; (c) THOR MB model

Model Update

During the THOR FE model rollover simulation, several problems in the original FE model were identified and the original model was updated accordingly. This included inaccuracy of the material property assignment (e.g. the deformable parts of the dummy: pelvis flesh, deformable joint rubber) and incorrect modeling of the THOR dummy component (e.g. steel cables in the deformable thoracic and lumbar joints were not modeled). In addition, the original shell element jacket was replaced with a solid element jacket to better model the interaction between the dummy and the rollover buck seat. Finally, the biggest issue identified in the THOR FE model during the rollover simulation was that the original THOR FE model resisted large lateral rotational motion. This unrealistic behavior was related to the DEPTH parameter in the contact algorithm under keyword *CONTACT_AUTOMATIC_SINGLE_SURFACE. This parameter is related to search depth in the search-based contact with contact

option SOFT=2. Per LSTC recommendation, this problem was solved by changing the contact parameter DEPTH from 5 (the original value used in the model) to 0 (default to 2). It should be noted that by default LSTC recommends the SBOPT=3, DEPTH=5 values for contact that includes edge-edge treatment in non-airbag contact problems. These values were used in the original THOR dummy model; however this setting did not work well in the rollover condition presented in the current study.

THOR MB Model and Update

The THOR MB model (v1.0) used in this study (Figure 1c) was developed by TNO and represents the THOR Mod Kit hardware dummy [28]. The model represents the THOR dummy geometry using ellipsoid surfaces, and meets most of the THOR dynamic certification requirements [28].

Model Update: The contact algorithms available for ellipsoid surfaces in MAYDMO software are less accurate than those for facet surfaces. In addition, the facet surfaces could represent the actual dummy geometry more accurately. Therefore, the ellipsoid surfaces in the original THOR MB model were replaced with facet surfaces, which were based on re-meshing (for reduced mesh density: element size ~20-30mm) of the THOR FE model mesh (Figure 2).

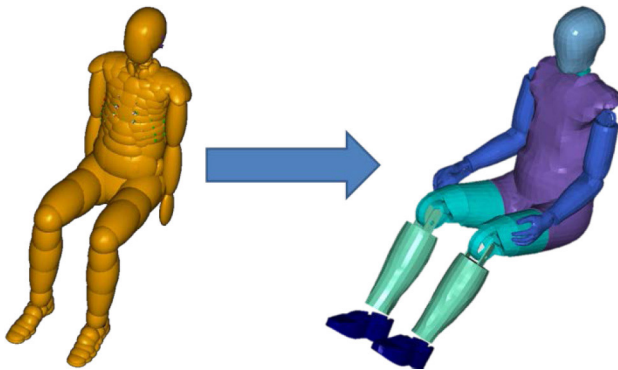


Figure 2. MADYMO THOR model with facet surface (based on the ellipsoid model)

THOR FE and MB Model Simulation

Zhang et al. presented the kinematic responses of the THOR dummy (along with five other dummies) in laboratory rollover tests [24]. These tests were performed with the Dynamic Rollover Testing System (DRoTS) as shown in Figure 3, which was designed to perform controlled and repeatable laboratory rollover crash tests [9]. The DRoTS was used to drop/catch and rotate a rollover test buck around its center of gravity (cg) without roof-to-ground contact. The test buck replicated the occupant compartment of a typical mid-sized SUV developed for the US market, and was designed to approximate a vehicle-based restraint environment with simplified boundary conditions (constructed mostly of steel), which allowed ease of test reproducibility and the facilitation of computational modeling [29]. Using a 3D optic motion tracking system, the dummy whole-body kinematic responses were characterized under the pure-dynamic roll condition with 35 newton belt pretension, at both front seating

locations (trailing-side vs leading-side). A repeat test was also performed for each dummy at each test condition for response repeatability evaluation.

The THOR dummy test condition used for the THOR MB and FE model evaluation in this study was the PR-L test, which is a pure-dynamic roll test (360 degree/s roll rate) with the occupant seated in the leading side position and 3-point belted (without retractor) with 35 newton pretension force (Figure 4). The rollover buck roll rate and roll angle time histories are presented in Figure 5.

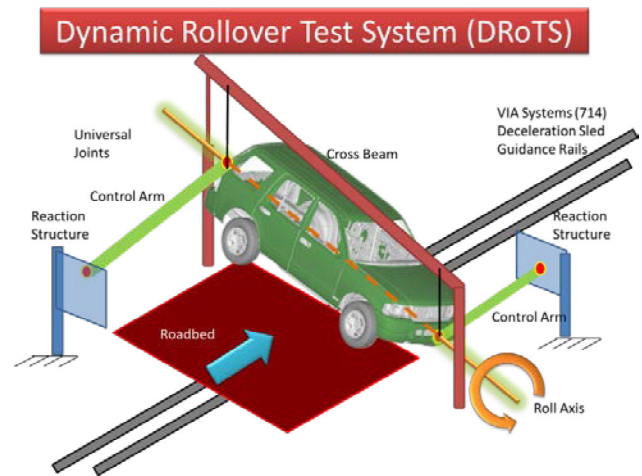


Figure 3. The Dynamic Rollover Testing System (DRoTS).

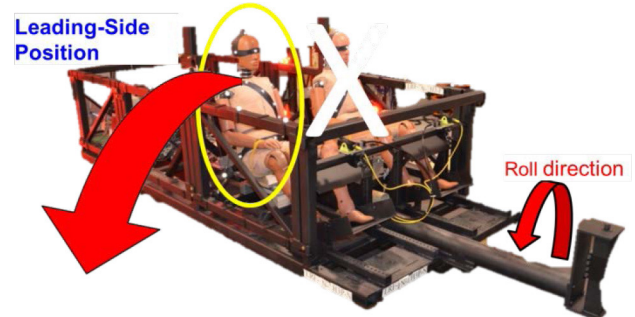


Figure 4. The rollover test buck

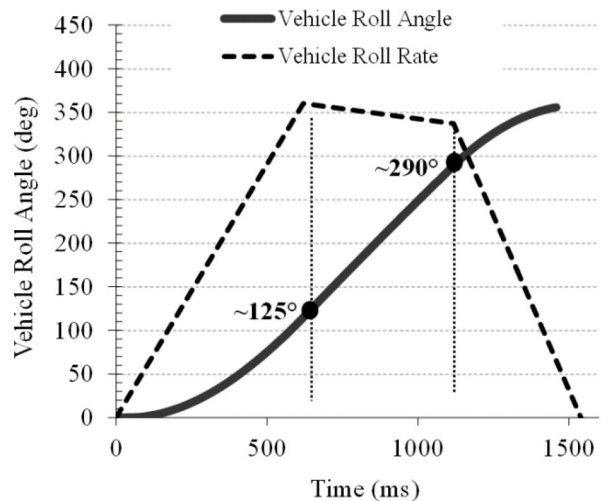


Figure 5. The rollover buck test rotational angle and rate time history

A rollover buck model (including the seat) was developed in both the MADYMO and LS-DYNA software respectively based on the CAD drawing of the rollover buck used in the tests (Figure 6). Both the THOR MB and FE models were then seated and positioned similarly in the buck models as in the tests. Pre-simulations were run to settle both THOR models down by applying gravity. Both models were then belted and a pretension force of 35 Newton was applied at both the outboard ends of the shoulder and lap belts. For MADYMO, this was achieved directly with the force actuator feature by specifying a target force (35 Newton), while for LS-DYNA, the target pretension was achieved indirectly by using the *BOUNDARY_PRESCRIBED_MOTION feature to specify a target prescribed displacement history. Therefore, a trial-and-error process was involved in obtaining the right amount of displacement to achieve the targeted 35 newton force. By the end of the belt pretension, the end points of the belts were switched to rigid and attached to the buck structure to simulate the 3-point-belt restraint (no-retractor). After the belt pretension loading, the CG roll rate time history measured from the test (Figure 5) was applied to both the MB and FE rollover buck models to simulate the buck rotational motion (Figure 5). The friction coefficients and seatbelt webbing in both of the MB and FE simulations were defined and referenced from the THOR FE model frontal gold standard simulations [30] as the following: 1) 0.2875 friction coefficient between the occupant and seat belt; 2) 0.2875 friction coefficient between seat belt and seatbelt slip ring; 3) 0.4 friction coefficient between dummy and buck interior structure; 4) Young’s modulus 4.2GPa for seatbelt webbing property.

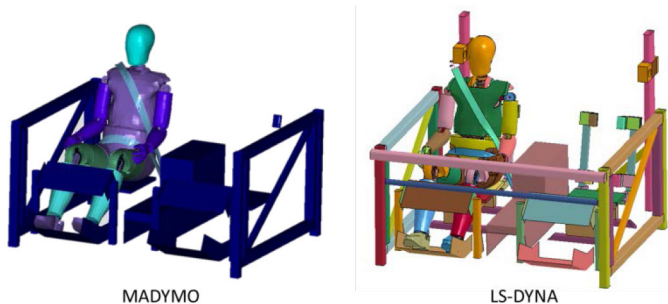


Figure 6. THOR MB and FE models in the Rollover Buck Model

In the experiment, kinematic measurement markers were placed on the THOR dummy at locations corresponding to the head, T1, T4, T10, L1 and Sacrum. The markers were attached to the dummy models at similar locations as in the experiment for the output of the kinematic motion of both the MB and FE THOR dummy models. For kinematic response comparison/assessment, both the occupant spinal 3D displacement (@ head, T1, T4, T10, L1, Sacrum) relative to the buck local coordinate system (following SAE vehicle coordinate system convention), and inter-spinal segment flexion/extension and lateral bending rotations (@head-T1-T4, T1-T4-T10, T4-T10-L1, T10-L1-Sacrum) were calculated for each simulation (Figure 7).

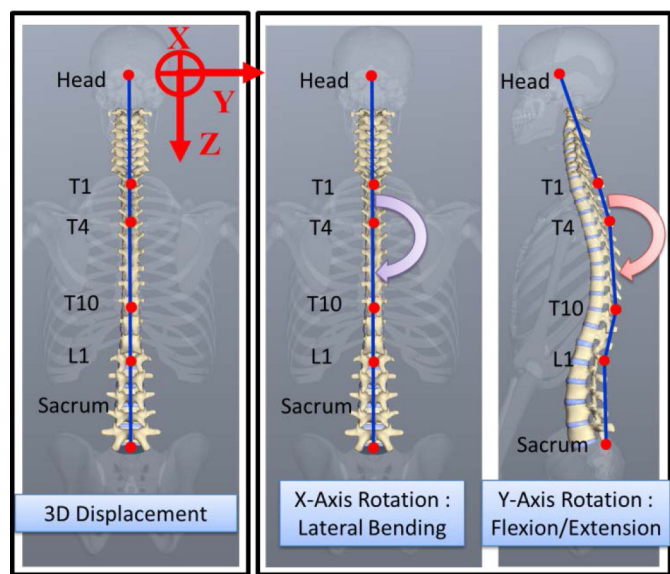


Figure 7. Occupant kinematic responses: 3D displacement and Inter-Spinal Segment Bending

Simulation Parameter Sensitivity Study

While extensive care was taken to minimize uncertainties of the experimental conditions for a well-controlled test protocol and to facilitate computational modeling of the tests, some parameter uncertainties still exist. Therefore, to ensure the robustness of the model validation, the sensitivities of the THOR dummy computational model responses to the parameters with uncertainty in the experiment were assessed. This included the seatbelt pretension force, friction between the occupant and the buck interior, and dummy seating posture. A matrix of 5 simulation conditions (Table 1) was performed for both the THOR MB and FE models for model response sensitivity assessment. The belt tension was evaluated at 35 Newton (target pretension force in experiment, baseline belt pretension) and 0 Newton. This was done in order to account for the viscoelastic relaxation of the dummy flesh that occurred after the pretensioning force was set to 35 Newton, and before the experiment was actually initiated. The friction coefficient was evaluated at three levels: 0.1, 0.4 (baseline) and 1. Finally two postures were evaluated: more upright posture 1 (baseline posture) and more reclined posture 2. The posture 2 is simply a result of around 3.5 degree pitch rotation of the head-neck-torso of the dummy in posture 1 in the rearward direction (Figure 8).

Table 1. Sensitivity Simulation Matrix for both the THOR MB and FE model

MB Simulation ID	FE Simulation ID	Belt Pretension (Newton)	Friction Coefficient	Posture
SIM7	SIM22	35	0.4	1
SIM11	SIM14	35	1	1
SIM8	SIM32	0	0.4	1
SIM12	SIM42	0	0.1	1
SIM13	SIM52	35	0.4	2

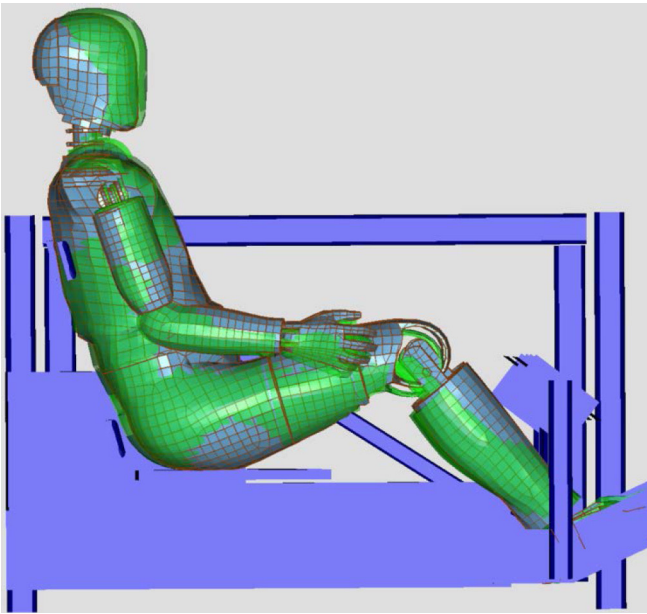


Figure 8. Dummy posture 1 (Green color dummy) vs posture 2 (Blue color dummy)

Dummy Computational vs Experimental Response Correlation Analysis

To quantitatively assess the similarity of the responses of the THOR dummy computational models relative to the physical THOR dummy, the cross-correlation method in the CORrelation and Analysis (CORA) method [31] was used. The cross-correlation method assigns three values to each pair of curves compared: (1) a magnitude coefficient, which has a value of 1.0 for identical magnitudes, and values less than 1.0 representing one signal's magnitude as a percentage of the other; (2) a shape coefficient, which does not have units and can have values between 1 (same shape) and 0 (no correlation); and lastly (3) a phase shift, which is expressed in units of time, describing the phase shift between two signals. They are calculated as in Table 2.

Table 2. Cross-Correlation Analysis

Definition: $P(x, y, h) = \frac{\int_{-\infty}^{\infty} x(t)y(t+h)dt}{\sqrt{\int_{-\infty}^{\infty} x^2(t)dt} \sqrt{\int_{-\infty}^{\infty} y^2(t)dt}}$
$Magnitude_Coefficient = \frac{\ x\ }{\ y\ } = \frac{\int_{-\infty}^{\infty} x^2(t)dt}{\int_{-\infty}^{\infty} y^2(t)dt}$
$Phase_Shift = h$ such that h maximizes $p(x, y, h)$
$Shape_Coefficient = p^2(x, y, Phase_Shift)$

Since results presented in Zhang et al. (2014) included two tests for each test scenario (for test-to-test repeatability assessment), correlation analysis was conducted between the responses of computational models and the responses of two experimental tests separately. In addition, correlation analysis was also conducted between the two repeat tests for THOR test-to-test repeatability assessment.

THOR Dummy Kinematic Response Biofidelity Benchmark

To benchmark the biofidelity of the THOR dummy under rollover condition, its kinematic responses (both the physical dummy and numerical models) were evaluated relative to PMHS response corridors published previously [23], using both the CORA rating method and the BioRank rating method [32].

The CORA method consists of both the cross correlation method and corridor method. The cross correlation method as presented above was used to assess the similarity of dummy responses relative to the PMHS/human average responses. However, for the corridor method, both the PMHS/human average responses and inner/outer corridors must be defined. The corridor method assigns a score of 1 if the evaluated curve is within the inner corridor, a score of 0 if the evaluated curve is out of the outer corridor, and linearly decreases from 1 to 0 if the evaluated curve is in between the inner and outer corridors (Figure 9). In the current analysis, the inner and outer corridors were defined to be the average experimental response $\pm \alpha$ standard deviation (S.D.), where $\alpha=1$ for the inner corridor and $\alpha=2$ for the outer corridor.

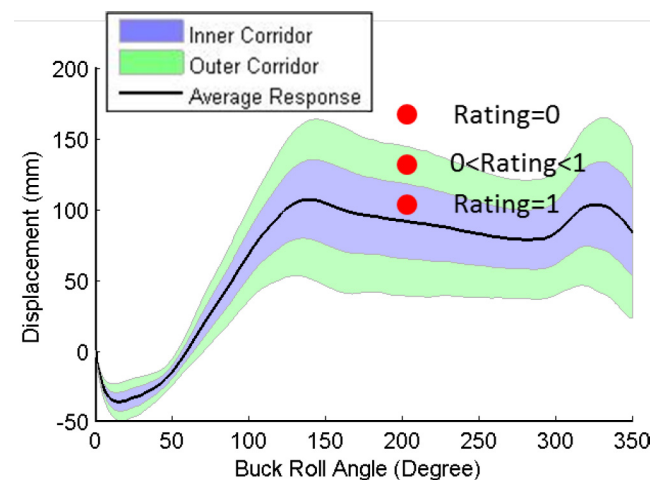


Figure 9. The corridor method in CORA

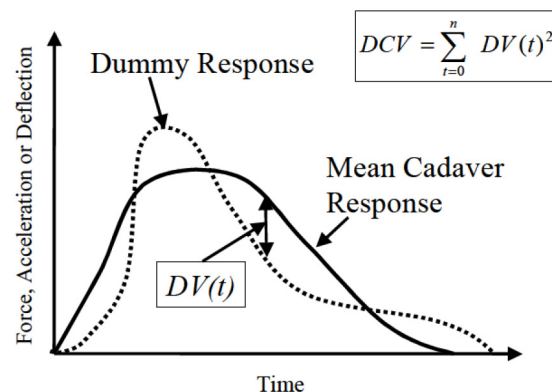


Figure 10. Dummy Cumulative Variance (DCV)

The BioRank method calculates the ratio of the cumulative variance between the dummy response and average human response (DCV) as in Figure 10 over the cumulative variance between the mean human response and the mean plus one standard deviation (CCV) as in

Figure 11. Therefore, essentially, the differences between the dummy responses and average human responses (DCV) are weighted by the variability/width of the human response corridors (CCV). A higher BioRank value indicates a poorer biofidelic response [32].

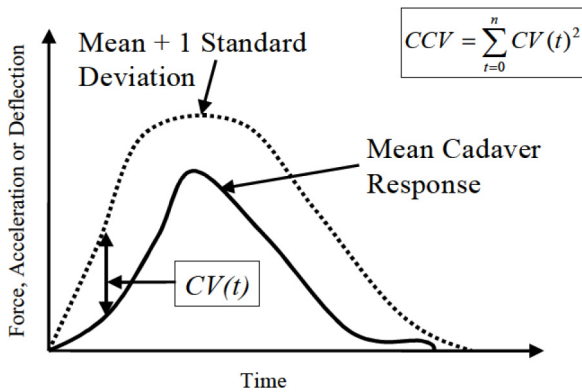


Figure 11. Cadaver Cumulative Variance (CCV)

For all of the analysis here, both occupant spinal 3D displacement (@ head, T1, T4, T10, L1 and Sacrum) and spinal inter-spinal segment flexion/extension and lateral bending rotations (head-T1-T4, T1-T4-T10, T4-T10-L1, and T10-L1-Sacrum) were considered. The calculated values for all data channels were averaged to obtain an overall value ($\pm$ S.D.) for each method (cross-correlation values, CORA and BioRank).

RESULTS

The kinematic responses of both the THOR MB and FE models were compared with the THOR experimental tests and PMHS response corridors at the same condition (pure-dynamic roll test at 360 degree/s roll rate with the occupant seated in the leading side position). The kinematic responses were plotted relative to the buck roll angle to facilitate an understanding of the occupant response data. Figure 28 (in Appendix A) shows the comparison of the occupant 3D displacement relative to the vehicle buck local coordinate system at the head, T1, T4 and T10 locations. Figure 29 (in Appendix) shows the comparison of the occupant 3D displacement relative to the vehicle buck local coordinate system at the L1 and Sacrum locations. Figure 30 (in Appendix) shows the comparison of occupant spinal inter-segment rotations at T4 (between T1-T4 and T4-T10), T10 (between T4-T10 and T10-L1) and L1 (between T10-L1 and L1-Sacrum) locations.

Similar to the THOR dummy kinematic tests, both the THOR MB and FE models demonstrated three dimensional motions relative to the buck. The centrifugal accelerations resulted from the buck rotational motion pulled the occupants both outboard (along buck local Y-axis) and upward (along buck local Z-axis). In addition, this upward and outward motion was also accompanied with the dummy lateral rotation about the buck local X-axis in the coronal plane and the occupant pitch rotation about the buck local Y-axis in the sagittal plane. Lastly, the occupant also demonstrated longitudinal translational motion (along the buck local X-axis).

Both the THOR MB and FE models in the simulation exhibited relatively similar lateral outboard excursion at the different anatomical levels, due to the lack of inter-spinal segment bending. This behavior matched well with the THOR experimental responses, but was in contrast to the PMHS responses, where the lateral excursion in general decreased in magnitude from the superior anatomical location to the inferior locations. The differences between the responses of the THOR dummy and the PMHS were due to the much higher spinal stiffness in the THOR dummy than in the PMHS in the lateral bending direction. Therefore, the THOR dummy (both computational and experimental) had much less lateral excursion than the PMHS at the superior kinematic measurement locations (e.g. more than 150 mm at head and 50 mm at T1 for leading side occupants), while the differences at the inferior locations were much smaller.

In the simulations, both the THOR MB and FE models also tended to pitch forward as a whole, as the centrifugal accelerations resulting from the buck rotational motion pulled the occupants upward. This is because while the centrifugal acceleration pulled the occupant upward away from the buck, the seat belt (mainly the lap belt) held the occupant onto the buck. These two loading mechanisms created a torque about the buck local Y-axis, and caused the dummy torso forward pitching motion. However, responding to the centrifugal acceleration, the PMHS straightened its upper spine (above T10) by pitching rearward while the spine below T10 pitched forward. These variations in spinal Y-axis rotation (pitch) response were the result of the much stiffer spinal response in flexion/extension direction in the THOR dummy than in PMHS.

Therefore, the THOR dummies (both the computational and experimental) and the PMHS exhibited opposite X-axis displacement at anatomical locations above T10: the THOR dummy displaced forward while PMHS displaced rearward.

THOR Computational Model Response Validation-Cross-Correlation Analysis

The Magnitude (Figure 12), Shape (Figure 13) and Phase Shifts (Figure 14) were the average coefficients from the cross-correlation analysis of all measurement channels for each cross-correlation analysis case. This includes 1) the THOR MB model kinematic responses (THOR MB baseline simulation 7 as in Table 1) relative to the responses of two THOR dummy tests respectively, 2) the THOR FE model kinematic responses (THOR FE baseline simulation 22 as in Table 1) relative to the responses of two THOR dummy tests respectively, and 3) kinematic response correlation between the two THOR dummy repeat tests.

In general, there was a relatively higher level of correlation in terms of the Shape coefficient (all over 0.85) than the Magnitude coefficient (between 0.5 and 0.7). The Phase Shifts between the THOR FE model responses and experimental results were almost zero, while the Phase Shifts between the THOR MB model response and experiment results were much larger but still under 25ms. One reason for the lower value of the magnitude coefficient is the spikes/noises that existed in the experimental data.

In general, the correlation value between the two experiments was higher than the correlation value between the THOR FE model and the THOR experiments, with the correlation value between THOR MB model and THOR experiment being lowest (Figure 12 and Figure 13). Finally, the correlation value between the THOR computational model and each THOR experiment is similar.

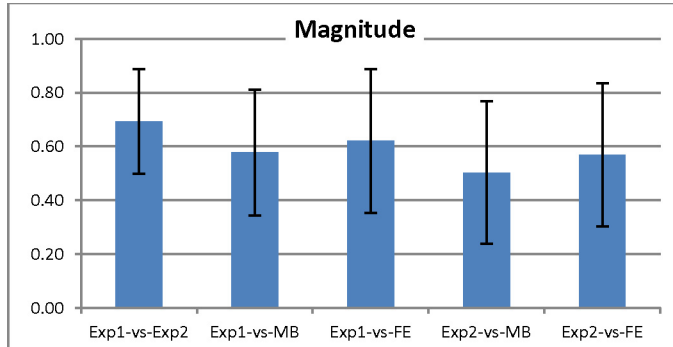


Figure 12. Magnitude Coefficient of Cross-Correlation Analysis between the THOR computational model simulations and THOR experiments; and THOR experiment repeat tests (Error bars indicated 1 standard deviation)

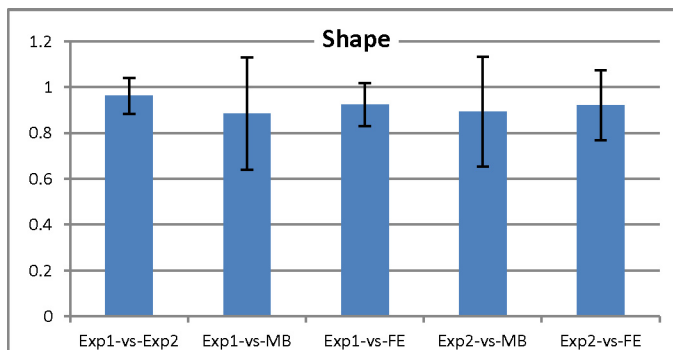


Figure 13. Shape Coefficient of Cross-Correlation Analysis between the THOR computational model simulations and THOR experiments; and THOR experiment repeat tests (Error bars indicated 1 standard deviation)

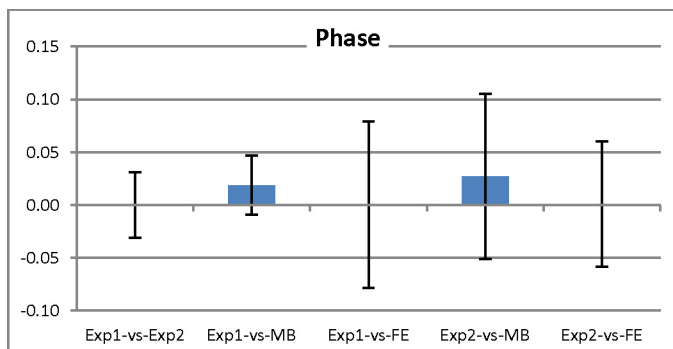


Figure 14. Phase Shift of Cross-Correlation Analysis between the THOR computational model simulations and THOR experiments; and THOR experiment repeat tests (Error bars indicated 1 standard deviation)

Response Sensitivity Analysis

Sensitivity to Belt Pretension

The comparison of all kinematic measurements between simulations with 35N seatbelt pretension (baseline) and the 0N seatbelt pretension is presented in the Appendix B. The results indicated that with lower

pretension force, the dummy tended to have larger Z-axis displacement (Figure 15). Since the viscoelastic relaxation of the dummy skin flesh inevitably happened during the experiments, the actual seatbelt force is somewhere between 0N and 35N. This explains to some degree the relatively smaller Z-axis displacement observed in the baseline THOR MB and FE simulations compared with the THOR experimental tests (Figure 15)

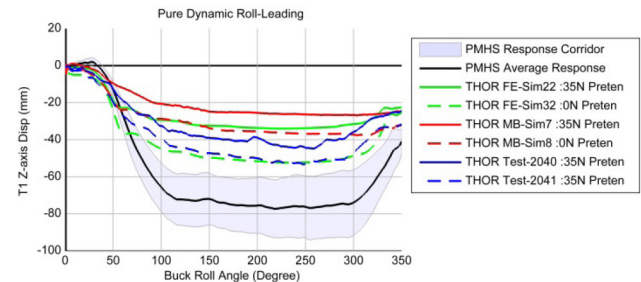


Figure 15. The effect of Belt Pretension on T1 Z-axis displacement

Sensitivity to Friction

The comparison of all kinematic measurements between simulations with friction coefficient 0.4 (baseline) and friction coefficient 1 is presented in the Appendix C. The comparison of all kinematic measurements between simulations with friction coefficient 0.4 (baseline) and friction coefficient 0.1 is presented in the Appendix D. Both indicated that with smaller friction coefficient, the dummy tended to have larger Z-axis displacement (Figure 16, Figure 17).

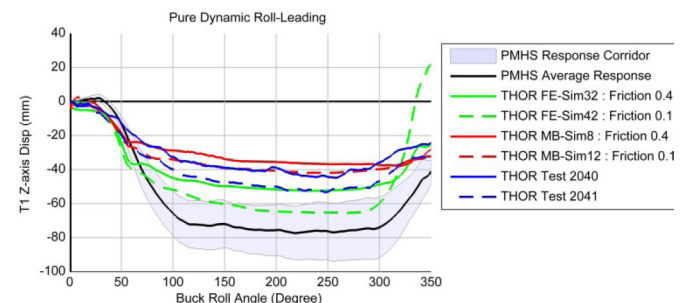


Figure 16. The effect of friction on T1 Z-axis displacement- (0.4 vs 0.1)

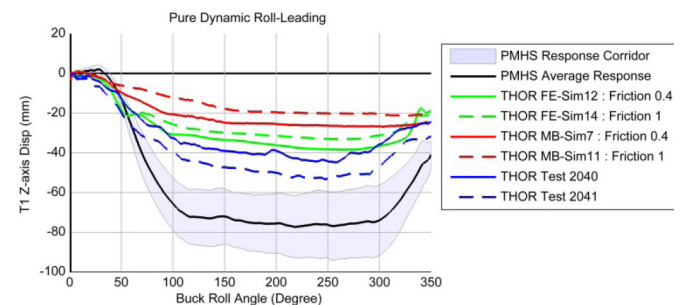


Figure 17. The effect of friction on T1 Z-axis displacement- (0.4 vs 1)

Sensitivity to Seating Posture

The comparison of all kinematic measurements between simulations with Posture 1 (baseline) and Posture 2 (more reclined) is presented in the Appendix E. The results indicated that with a more reclined initial posture, the dummy tended to have larger Z-axis displacement